BRITISH WELDING JOURNAL

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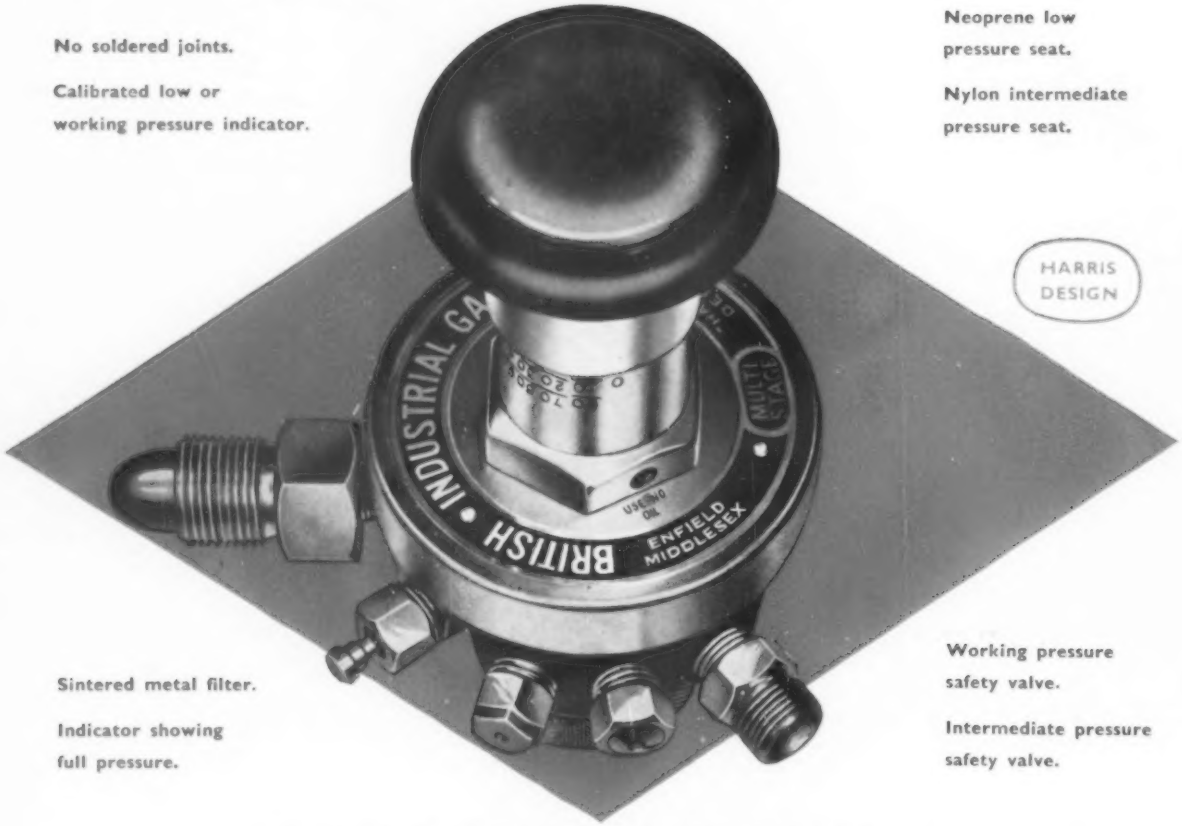

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Inside front cover

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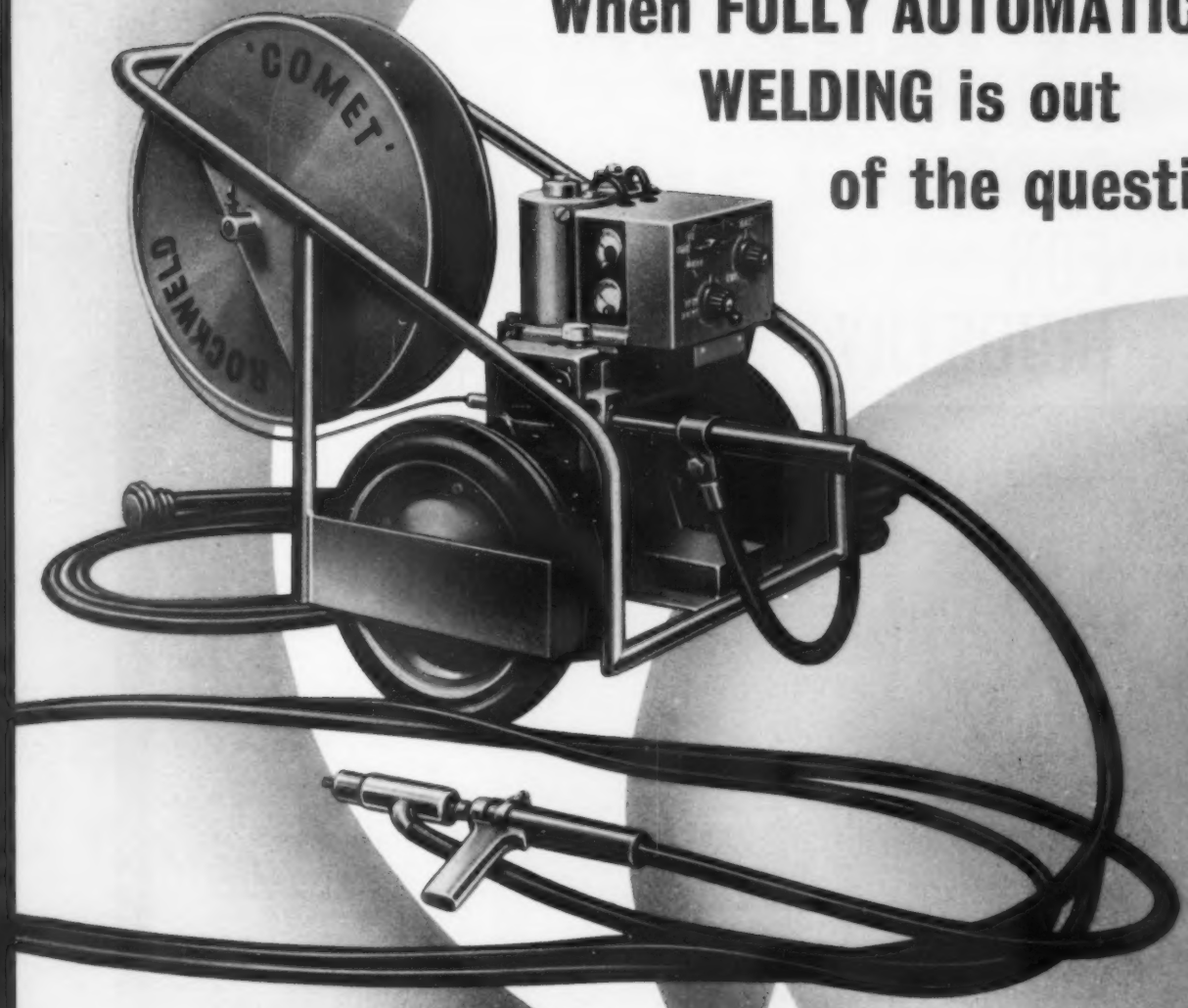

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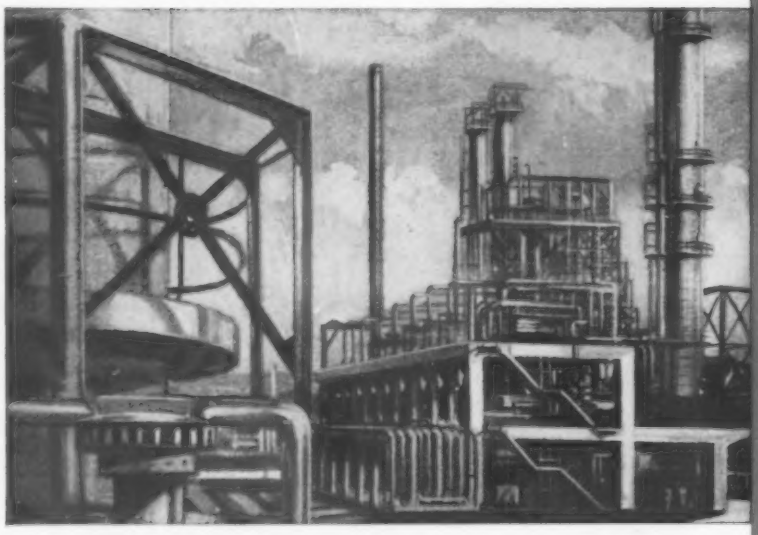

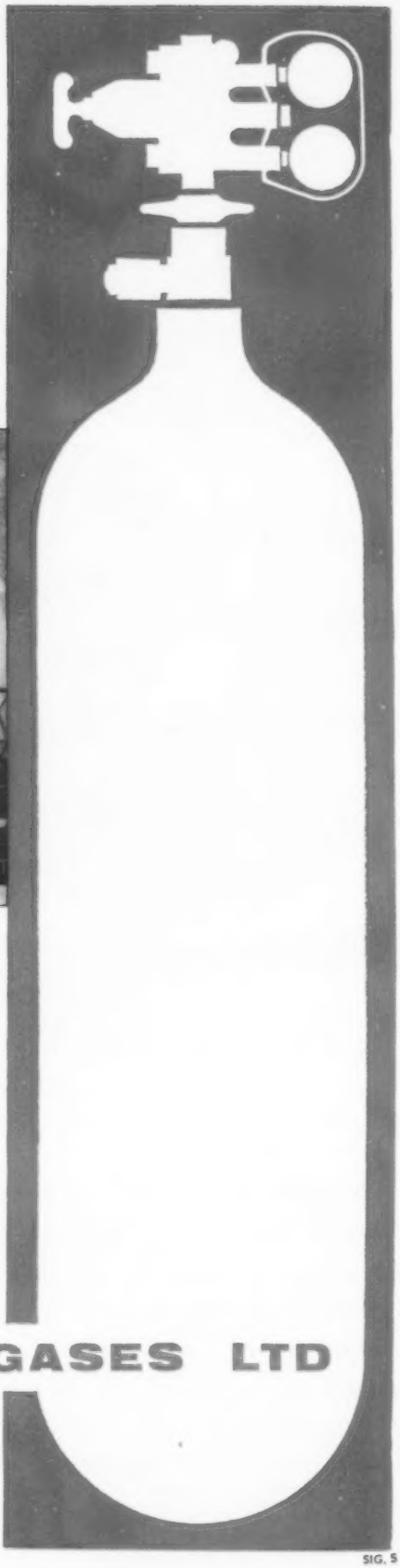

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SIG. 5

International NU-SWIFT News

No. 2

HAVE YOU MET THESE PEOPLE?

21. BRITISH EXPORTS of fire extinguishers are the highest in the world. Total value of NU-SWIFT exports is increasing continuously. Last year they amounted to NEARLY ONE-THIRD OF THE BRITISH TOTAL.

22. In arctic SPITZBERGEN (Norwegian: SVALBARD), divided by an invisible iron curtain, but where Norwegian and Russian workers fraternise, the NORWEGIAN COLLIERIES of STORE NORDISKE SPITSBERGEN KULKOMPAGNI are protected by Nu-Swift.

23. THE ROYAL CANADIAN NAVY, which in 1947 standardized on the Nu-Swift Universal Water/CO_2 Extinguisher, has placed further orders.

24. HAILE SELASSIE, H.I.M. of ETHIOPIA, is reported to take a keen interest in fire extinguishers. Nu-Swift has been approved and the first consignments have been sent to ADDIS ABABA.

25. Since MOROCCO gained her independence, a Nu-Swift sales concessionaire has been appointed and the first supplies of extinguishers have been shipped to glamorous CASABLANCA.

26. Narrow and winding alleyways in the ancient town of ST. GEORGE, first British settlement in now fashionable BERMUDA, sometimes prevent the Fire Brigade from getting close enough for effective fire fighting. Fire engines are therefore fitted with extinguishers to enable firemen to fight inaccessible fires with reliable Nu-Swift.

27. A.T.A.C. the MUNICIPAL TRAFFIC COMBINE of the City of ROME, and reputed to be the largest surface passenger transport organization in Europe has decided, after tests extending over two years, to standardize on Nu-Swift.

28. YUGO-SLAVIAN STATE-OWNED CORPORATIONS are among the Nu-Swift customers situated on the edge of the East-West iron curtain.

HAVE YOU BEEN TO THESE PLACES?

29. In HELSINKI, capital of gallant little Finland, and almost within a taxi ride of Leningrad, the FINNISH POSTS AND TELEGRAPHS DEPARTMENT have decided to standardize Nu-Swift Dry Powder extinguishers, especially suitable for use under severe arctic conditions, common each winter in Northern Finland. Substantial orders have already been placed.

30. GERMAN SHIPOWNERS are now also able to fit Nu-Swift in their ships, thanks to the approval of the SEE-BERUFSGENOSSENSCHAFT in Hamburg.

31. At TULLN, Austria, in the presence of high ranking Austrian fire fighting officials, including BRANDDIREKTOR DUFEK, Chief Fire Officer of Vienna, two Nu-Swift Dry Powder Extinguishers, Model 1604, bonded for 2 years for reliability tests were recently discharged. They passed with flying colours and OBERBRANDRAT SPEIL, the official responsible, signed the certificate of approval on the spot.

32. The Government of the FALKLAND ISLANDS, British sheep-farming colony in the South Atlantic, near Argentina, recently placed their first Nu-Swift order. Sheep vastly outnumber human inhabitants in the Colony, previously of great strategic importance, but GREASY WOOL has high fire risk.

33. INDEPENDENT TELEVISION AUTHORITY, always in keen competition with the BBC, is determined that programme transmission shall never be interrupted through preventible causes. All its station are now protected by Nu-Swift.

34. Reputedly being let at £25 a day or more, NEW LUXURY SUITES on the top floor of the DORCHESTER, London's premier Park Lane Hotel, are protected by Nu-Swift.

35. The up-to-date works of ALFA ROMEO, the well-known motor car manufacturers in NORTHERN ITALY, have recently been equipped with Nu-Swift. Crash tenders too have been fitted with fast and reliable Nu-Swift.

36. In NASSAU, BAHAMAS, eldorado of all YACHTING AND DEEP-SEA FISHING enthusiasts, many of the YACHTS are equipped with Nu-Swift.

37. PRINCESS BENEDIKTE, teenage daughter of the King of Denmark, recently launched a large train ferry named after her at ELSINORE close to the legendary home of Hamlet. Required urgently by the DANISH STATE RAILWAYS to cope with increase in cross-water traffic, the ferry was equipped with Nu-Swift.

38. The Trustees of SHAKESPEARE'S Birthplace in STRATFORD-ON-AVON, conscious of the dollar-earning value of the Bard's home, has protected the property with Nu-Swift. Last year the house was visited by over 200,000 sightseers.

39. H.M. THE KING OF SWEDEN has had Nu-Swift fitted in his cars and other vehicles. On board H.S.M.S. 'TRE KRONOR', Sweden's premier man-of-war, His Majesty is similarly protected. Three other reigning Sovereigns are also protected on board their yachts by Nu-Swift.

40. ESSO RESEARCH LTD. have equipped their new offices at ABINGDON, BERKS, ENGLAND, with Nu-Swift.

41. The UNITED KINGDOM ATOMIC ENERGY AUTHORITY have protected DOUNREAY EXPERIMENTAL REACTOR ESTABLISHMENT in remote CAITHNESS in the North of Scotland with reliable Nu-Swift. The U.K.A.E.A. has some fire risks: the fire in 1957 at their Windscale No. 1 pile cost £1,967,826.

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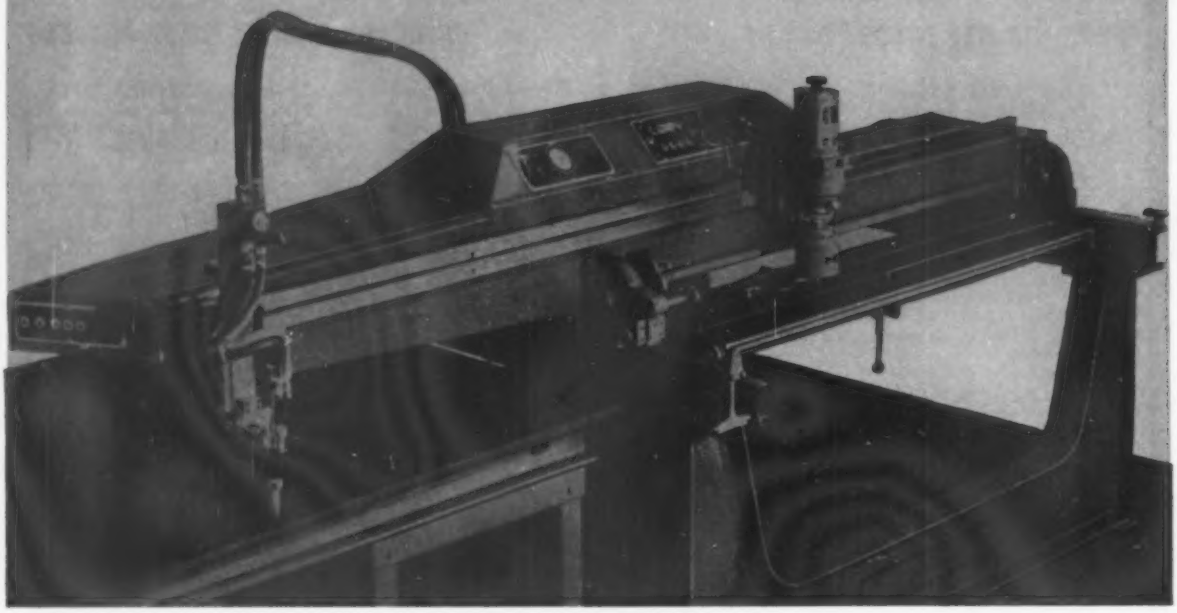
HANCOMATIC OXYGEN CUTTING MACHINE



Now available in 90" & 120" cutting widths

Hancomatics! Today's finest range of dual carriage oxygen profiling machines. In addition to machines with 40" and 62" cutting widths, there are now Hancomatics with 90" and 120" cutting widths. Hancomatic means top speed, precision cutting — every time; as clean as nearly 40 years specialist experience in cutting can ensure.

NEW sleek enclosed machine tool appearance, protecting moving parts from dirt and dust.

NEW rigid box construction.

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NEW tracer head for operation from all types of template.

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The new Hancomatic embodies the best principles of oxygen cutting in a handsome, enclosed streamlined machine. Take a big step towards streamlining your production. *Write for details:*

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Electronic HANCOLINE
traces from pencil drawing *automatically*

★ Cut width compensation — on tracer head.

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Available with 38", 60", 90" and 120" cutting widths.

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TIB. 169

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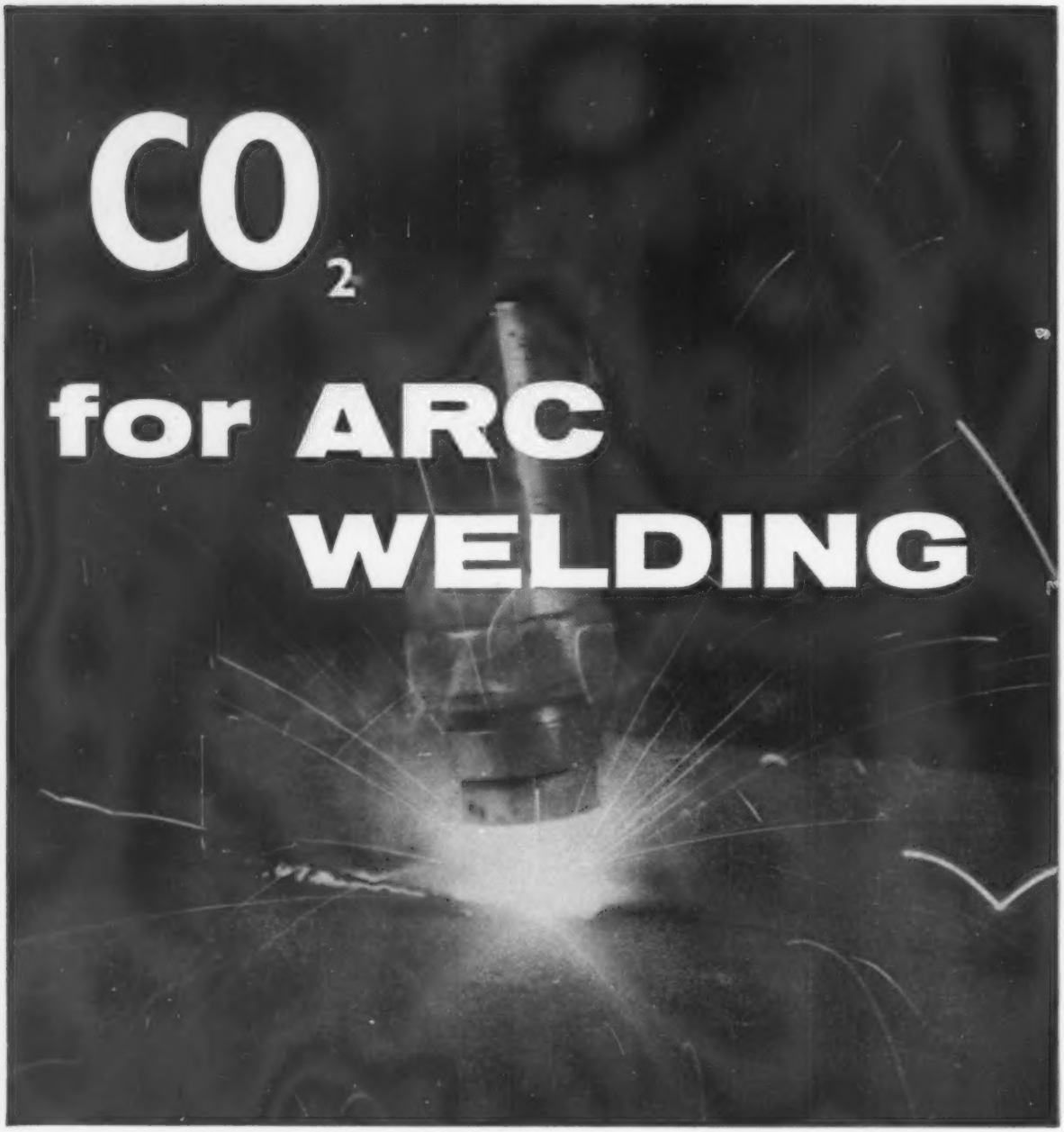

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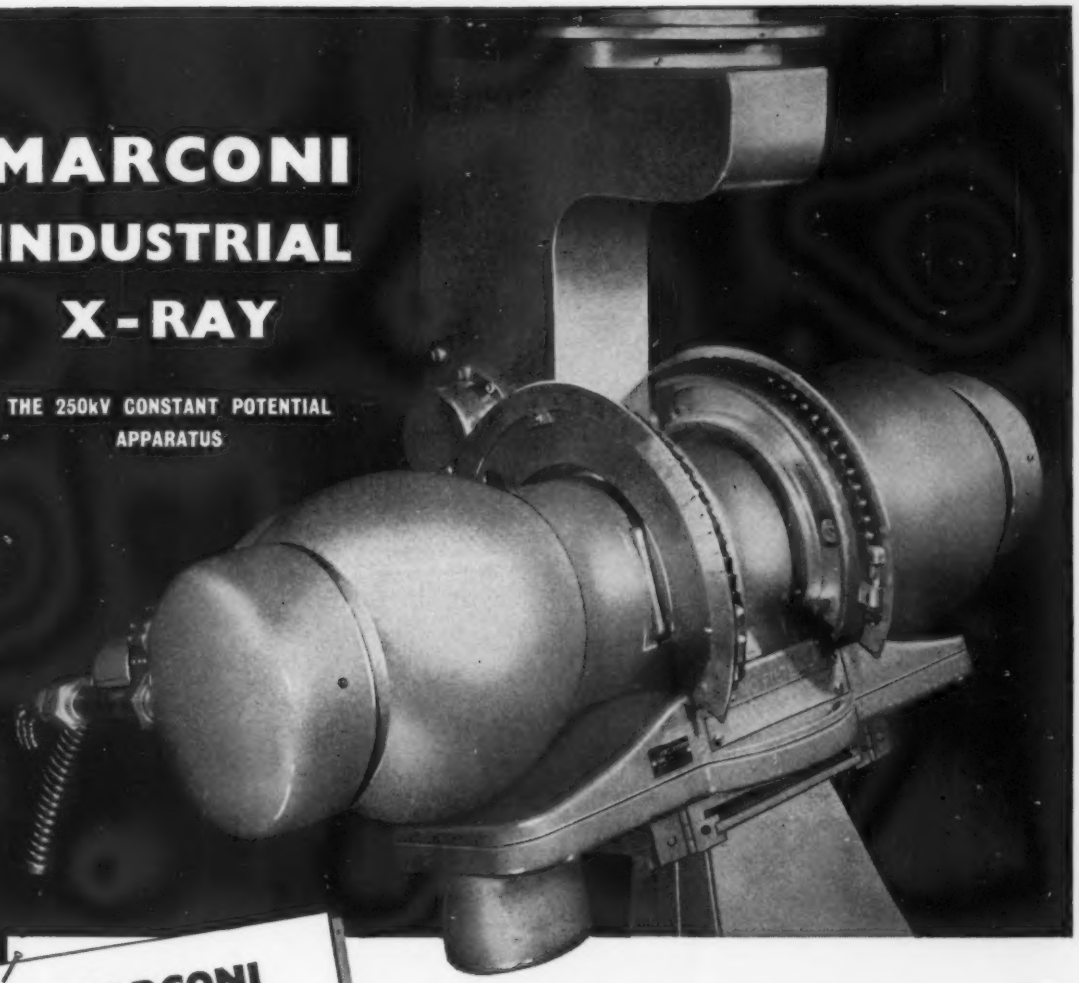
MARCONI INDUSTRIAL X-RAY

THE 250kV CONSTANT POTENTIAL APPARATUS



MARCONI for X-Ray

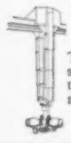
DESIGNED FOR A WIDE VARIETY OF APPLICATIONS IN THE ENGINEERING INDUSTRY

THE Marconi 250 kV Constant Potential X-Ray equipment is one of the most efficient and versatile 'inspection tools' at the service of the engineering industry. The basic apparatus comprises four main items—a high tension transformer-rectifier unit, a control unit, an oil circulator/cooler and the oil-cooled tubehead illustrated above.

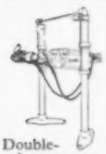
The Kilovoltage range—which is from 30 to 250 kV in 2-kV steps—permits examination of components varying from plastic and aluminium items to ferrous sections more than three inches thick.

The scope of the apparatus has been extended still further by the development of a comprehensive range of tubehead mountings, which includes fully mobile and semi-mobile units, and suspension and static tubestand types, some of which are shown on the left. These mountings have been designed to meet industrial requirements and Marconi engineers are always available to recommend the most suitable installation, or suggest adaptations. For preliminary details of the Marconi 250 kV Constant Potential X-Ray equipment, please write for leaflet AQ22.

SOME MOUNTINGS FOR THE 250-kV TUBEHEAD *



Telescopic-suspension tubehead mounting



Double-column gantry tubestand

Dual-travel tubestand

* *Let Marconi engineers help you in your choice of mountings. In addition to those shown, other tubehead mountings are available to suit specialized requirements. Full details gladly sent on request.*

MAY WE ALSO SEND YOU PARTICULARS ABOUT OTHER MARCONI INDUSTRIAL X-RAY EQUIPMENT?

Please address enquiries to MARCONI INSTRUMENTS LTD. at your nearest office:

London and the South:
Marconi House, Strand, London, W.C.2
Telephone: COVent Garden 1234

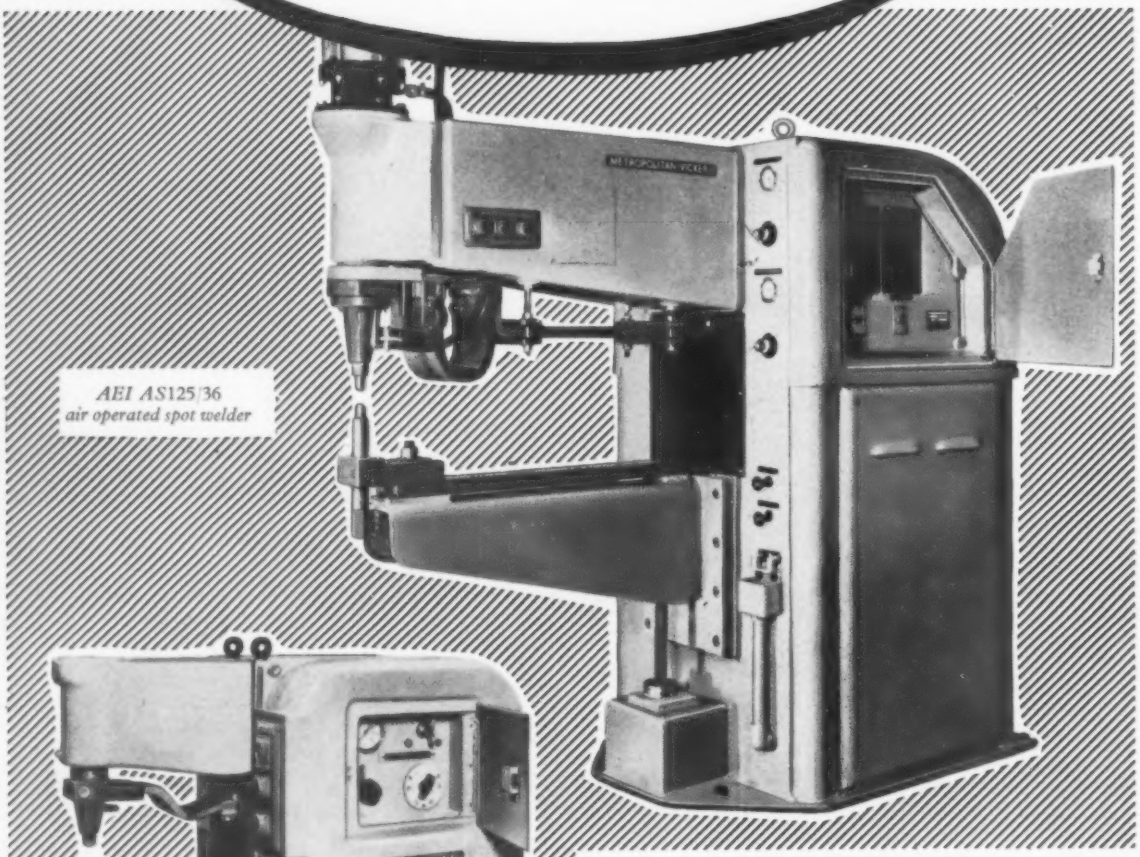
Midlands:
Marconi House, 24 The Parade, Leamington Spa
Telephone: 1408

North:
23/25 Station Square, Harrogate
Telephone: 67455

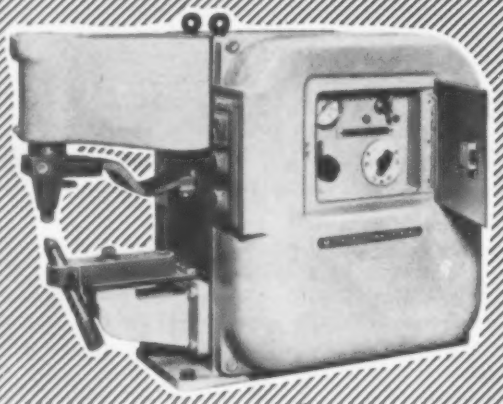
Export Department: Marconi Instruments Ltd., St. Albans, Herts. Telephone: St. Albans 56161

IX22

Choose from the AEI RANGE



AEI AS125/36
air operated spot welder



AEI AS 10/9 bench mounting spot welder

If you have exacting spot welding requirements, it will pay you to study the AEI range. The table on the opposite page gives performance figures for mild steel welding for the standard range — with well fitting assemblies, considerably greater thicknesses may be welded. Performance figures for other metals and alloys can be supplied.

All machines are normally available from stock.

AEI special multi-head machines are incorporated in the high speed production lines in many industries—motor car body building, refrigerator manufacture, etc.

AEI **Associated Electrical Industries Limited**

STANDARD SPOT WELDERS
Available from stock

	Type	Maximum welding rating*	Throat depth		Maximum welding force†		Maximum capacity
		kVA*	in	mm	lb	kg	
Pedal Operated	M196	4	3 & 6	76 & 152	—	—	2 x 0·036" (0·9 mm) & 2 x 0·028" (0·7 mm) Mild Steel
	M63	16	6 to 18	152 to 456	—	—	2 x 0·11" (2·8 mm) to 2 x 0·078" (2 mm) Mild Steel
	M194	32	12 to 30	304 to 760	—	—	
Air Operated	AS 10/9	16	9	228	250	113	2 x 18 s.w.g. (1·2 mm)
	AS 25/24	35	24	609	600	272	2 x $\frac{1}{8}$" (3·2 mm)
	AS 60/18	90	18	457	1,000	453	2 x $\frac{3}{16}$" (4·8 mm)
	AS 60/24	90	24	609	1,000	453	2 x $\frac{3}{16}$" (4·8 mm)
	AS 75/36	130	36	914	1,000	453	2 x $\frac{3}{16}$" (4·8 mm)
	AS 75/48	130.	48	1,218	1,000	453	2 x $\frac{3}{16}$" (4·8 mm)
	AS125/36	215	36	914	2,250	900	2 x $\frac{1}{4}$" (6·3 mm)
Stored Energy (Condenser discharge)	ASC40/36	—	36	914	2,000	906	2 x 14 s.w.g. Aluminium Alloy (2 mm)
Roller (Air operated)	AS100/24R	100	24	609	1,500	680	Dependent upon type of material and application
	AS150/36R	175	36	914	1,500	680	
	AS250/36R	400	36	914	2,250	1,020	

All mild steel capacities based on BS 1140 tests

* Ratings in line with BS 3065:1959

† At 80 lb/sq in (5·6 kg/sq cm) air supply

Send for this booklet

This illustrated booklet gives concise data on these machines and other AEI welding and heating equipment.

TRANSFORMER DIVISION — HEATING & WELDING DEPARTMENT

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LW/901

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A COMPREHENSIVE RANGE OF MACHINES CAN BE SEEN
IN OUR WELDING DEMONSTRATION WORKSHOP
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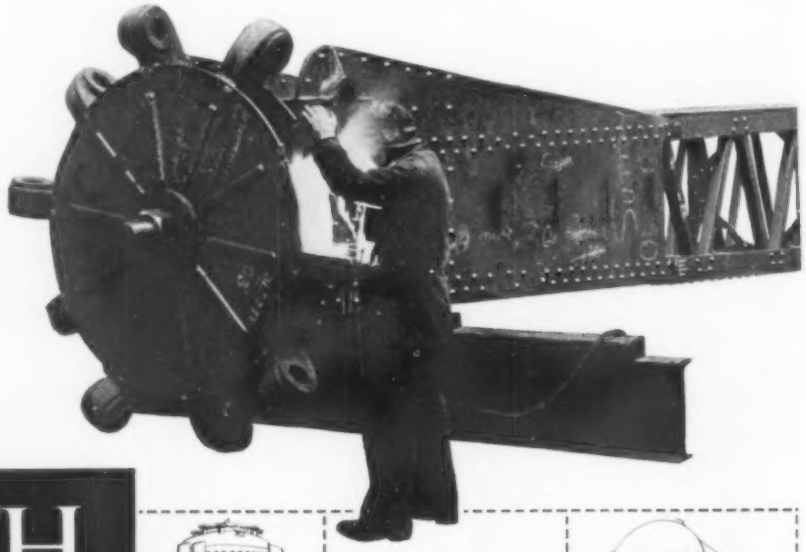
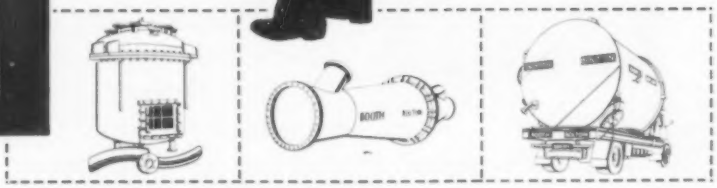
The photograph shows one of our welders at work on the all-welded crown plate for 150 ft. guyed derrick. Our greatly enlarged and improved welding facilities are at your disposal for the fabrication of all sizes of storage tanks, pressure vessels, and welding work to your own requirements



Welding by

BOOTH



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They're busy at Bedewell!

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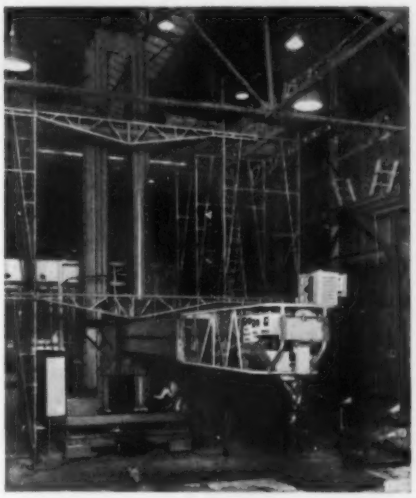
General view of one of the Bedewell shops.

These photos were taken recently at Bedewell Works, Hebburn-on-Tyne, the Baker Perkins Group factory where Yates Plant equipment is made. Here, a full range of patented welding manipulative equipment is manufactured, all with the latest exclusive refinements developed by Yates Plant and all built to the highest engineering standards.



Erecting a "mammoth" 45 ft. column and boom. This unit has been supplied for work in atomic power station construction.

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Welding equipment designers and maintenance engineers can now specify "Mullard" when ordering ignitrons . . . a Mullard range of six types with internationally available equivalents has recently been introduced.

Five are intended primarily for resistance welding, and make possible the exact control of extremely heavy currents to provide uniform and reliable welds. Thermostats may, of course, be fitted to provide protection against overheating or to economise in the use of cooling water. The sixth type in the range is designed specifically for controlled rectification at high power.

Brief details are given here—for further information please write to the address below.

TYPE No	SIZE	
5551A ...	B (600 kVA) ...	For 1 or 3-phase welding control
5552A ...	C (1200 kVA) ...	For 1-phase welding control
2052A ...	C (1200 kVA) ...	Special alternative C size
5822A ...	— ...	For 3-phase welding control
5553B ...	D (2400 kVA) ...	For 1 or 3-phase welding control
5555 ...	— ...	For controlled rectifier duty at 150A (continuous) with a P.I.V. of 2.1 kV.

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$$D = \sqrt{t}$$

The formula $D=\sqrt{t}$ for the relationship between electrode tip diameter "D" and work thickness "t" was devised by MALLORY. Welding engineers the world over have adopted it as a reliable aid to efficient operation.

Material is as important as design—and here again MALLORY leadership shows itself, for the superior properties of Mallory 3 are universally recognised.

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Booklet 1200 "Mallory Resistance Welding" is free on request.

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Strength Flexibility and versatility are important features of this International Drott Skid-Shovel, mounted on BTD-6 Crawler Tractor. Invicta Electrodes are extensively used in the fabrication of this unit, and they too offer you strength, flexibility and versatility, together with reliable weld deposits. The INVICTA range covers every arc welding application.

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Full details of the INVICTA range will gladly be sent on request

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AEI ELECTRONICS DATA SHEET
Subject IGNITRONS — RATINGS

Welder types

Types	Maximum Demand* (kVA)	Maximum average* anode current (Amps.)	Temperature control
BK 22	450	15	—
BK 24	1200	140	—
BK 24A	1200	140	Integral
BK 24B	1200	140	Clamp on
BK 34	2400	355	—
BK 34A	2400	355	Integral
BK 34B	2400	355	Clamp on
BK 42	600	56	—
BK 42A	600	56	Integral
BK 42B	600	56	Clamp on
BK 66	300	22·4	—

* Ratings are for welder control service and refer to two valves in inverse parallel at any voltage from 250-600v. r.m.s.

Rectifier types

Type	Maximum peak voltage (Kilo volts)	Maximum average current at peak voltage (Amps.)
BK 44	2·1	75
BK 46	2·1	150
BK 56 †	20·1	150

† Tentative ratings.

ASSOCIATED ELECTRICAL INDUSTRIES LIMITED
ELECTRONIC APPARATUS DIVISION LINCOLN, ENGLAND

A.5447

MILD STEEL

STAINLESS STEEL

NON-FERROUS MATERIALS

Head Wrightson Teesdale Limited have extensive experience in the design and manufacture of equipment for the iron and steel industry, the petroleum, chemical and nuclear industries. Additionally, their products are supplied to the railway and harbour authorities. This service is backed by a Class I Welding Shop with modern X-ray equipment suitable for examining materials up to 3" in thickness. Their skill in this field enables them to offer a testing service for their clients who may encounter new or difficult welding problems.

Welding Tube Ends in a Stainless Steel Cooler supplied for an Experimental Nuclear Reactor. Welding by Argon Gas Torch.

HEAD WRIGHTSON TEESDALE LIMITED

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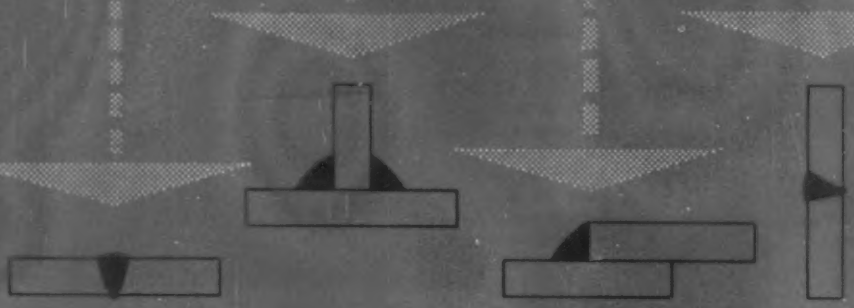

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Incorporating the *TRANSACTIONS* of the Institute and *WELDING RESEARCH*

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WELDED DESIGN AND CONSTRUCTION IN CORROSION- AND HEAT-RESISTING MATERIALS

21–25 MARCH 1960

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WELDED STRUCTURES

4–8 APRIL 1960

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Spring Meeting 1959

The Spring Meeting of the Institute of Welding was held in London from Monday, 20th April, to Friday, 24th April, 1959. The London Branches Joint Dinner was held at the Connaught Rooms on Tuesday 22nd. Works visits were arranged for the Tuesday and Thursday, and there was an official visit to the Engineering, Marine, Welding, and Nuclear Energy Exhibition at Olympia on the Friday.

There were two technical sessions on Wednesday and Friday for the presentation and discussion of papers. The Chairman at the first session was Dr. R. Weck (Director of the British Welding Research Association), and at the second session Mr. Gilbert Roberts (Freeman, Fox & Partners) presided.

First Session

Low Temperature Properties of Welded and Unwelded Al–5% Mg Alloy Plates* *By J. E. Tomlinson and D. R. Jackson*

Extruded Aluminium Alloys for Low Temperature Service* *By R. J. Durham*

Tear Tests on Aluminium-Magnesium Alloys* *By J. Sawkill and D. James*

Low Temperature Properties of Aluminium–Magnesium Alloys* *By R. E. Lismer*

The Chairman (Dr. **R. Weck**): The problem of brittle fracture of steel structures has been an extremely serious one and has been very worrying to those people who are concerned with the construction of such structures and the selection of materials for them. Perhaps in view of these facts, it is not surprising that engineers should have looked round for alternative materials to be used in the construction of these structures which would not be liable to suffer from this kind of failure.

Aluminium alloys and other light alloys do not, on the whole, suffer from this change of properties with temperature, and a field is opening up for welded aluminium structures on the basis of their perhaps superior notch ductility properties.

In a way, this morning is a unique occasion, because many papers have been presented to the Institute dealing with the problem of brittle fracture in steel. But I believe this is the first time that the same subject is being discussed in relation to light alloys.

Dr. **E. G. West** (Aluminium Development Association): The reason why four different laboratories were concerned with this problem is that there was no published information of value about low temperature properties of aluminium alloys at the low temperatures required for this new development. Between three and four years ago we began receiving enquiries, and from work then carried out it looked as though the properties would be satisfactory; but nearly all this work was on pure aluminium of varying purities.

The published work covered tensile tests almost exclusively, and although some of these had been taken down to about 4° absolute on pure aluminium, which showed quite considerable increase in tensile strength whilst retaining the ductility of normal temperatures, comparable information was needed for the types of alloy, which could be used economically for storage tanks. It was necessary to investigate particularly their tendency to tearing.

The work was undertaken independently by the four laboratories and it is interesting to note the close agreement reached, using essentially the same tests but different techniques and different equipment.

It would have been useful if we could have done some work on these or comparable alloys, in the cast condition, for designers, sooner or later, will want to use more castings in such equipment, and we must know their properties at these temperatures.

It would also be helpful to have the properties of castings as a means of assessing the properties of welds, because, after all, welds are only chill castings with rather excessive cooling rates and very pronounced directionality.

The importance of the crystallographic structure, the space lattice arrangement of the atoms, is evident from Mr. Tomlinson's film. It would be interesting to undertake specific experimental work to see just how changes in the lattice dimension and in the lattice type would affect properties.

Most other metals have face-centred cubic lattices, and it would also be interesting to have any comments or perhaps unpublished information on some of these other materials.

Mr. **M. Birkhead** (W. P. Butterfield & Co. Ltd.): Mr. Lismer states that, in general, maximum values of strength and ductility are attained at a temperature of about −150°C., but I should like to have the comments of any of the authors on the properties or expected properties between −196°C. and absolute.

In the same way I should welcome more information on the weld deposits as distinct from the parent material, especially in view of Mr. Lismer's conclusions that weld deposits have been shown to be of considerably lower strength than parent plate materials.

Would Mr. Tomlinson care to comment a little more fully on the relative properties of stainless steel and aluminium and aluminium magnesium alloys under these low-temperature conditions?

Finally, methods of testing have been quoted, and I wonder whether the authors could help to correlate the test conditions a little more fully in relation to the Izod and Charpy impact tests and the tear tests described in the papers.

* *Brit. Welding J.*, 1958, vol. 5 (Nov.), pp. 499–510; 510–516; 517–522; 523–538.

Dr. **R. Hargreaves** (British Oxygen Research and Development Ltd.): Mr. Lismer's work on the NE.5 alloys shows that the more dilute aluminium-magnesium alloy appears to have what might be optimum general properties for low-temperature work in a wider sense than just its use for tanks at sea at −160°C.

The alloy is in an extruded condition and the energies to propagate fracture and the total energy to fracture were considerably greater than for the hot rolled materials described in the paper, though the maximum load to fracture was of the same order. The ductility of the hot-rolled materials was generally higher at about −150°C than at room temperature or at −196°C, but the tensile strength increased with decreasing testing temperature right down to −196°C. At the same time, the change in appearance of the structure from an oblique shear at room temperature to one normal to the plate took place at something less than −150°C., although at −196°C., it did become normal to the surface.

In comparison with alloys *A* and *B*, which are rolled material with a rather higher magnesium content, one can say that the extruded NE.5 retains what might be considered the optimum balance of the mechanical properties for low temperature service.

Mr. Lismer also referred to the appearance of beta-phase in grain boundaries in these alloys, and I suggest that problems of corrosion in the more concentrated magnesium alloys may arise, particularly in welded structures.

Mr. **G. M. Boyd** (Lloyd's Register of Shipping): This study of the low temperature properties of aluminium alloys is a very valuable piece of work, on which the collaborating laboratories are to be highly commended.

As Dr. West has said, if we look back only a few years, we find that we knew very little about these properties, and our ideas were rather naive, just as they were about mild steel at a slightly earlier stage. It is healthy that we are now looking more critically at the materials we are using for important structures.

I have been very interested in the mechanism of the fractures in these alloys, which is apparently different from the behaviour of steel as I have previously described.* In the aluminium fractures the 'chevron pattern' is not curved as in steel, and the fracture fronts were shown, by staining to be formed by two straight lines forming a triangle with its apex on the middle line, pointing in the direction of propagation.

This indicates that in the aluminium fractures, the process was continuous, and not intermittent as in steel. This important difference in mechanism is probably related to the fact that whereas the fractures in the aluminium were of the "stable" kind, those described in steel were unstable, or brittle.

The load/deflection diagrams are instructive in relation to this matter of stability. In Fig. *A* if, from the point B, at which fracture commences, we draw a sloping line B E, the slope of which is a reflection of the elastic line, then the triangle BCE represents the stored elastic energy at the moment when fracture commences. If, as the fracture extends, the load deflection curve continues in such a way that its slope is never steeper than the line B E, the fracturing will be stable, but if the slope becomes steeper than the line B E, the fracturing will be unstable. This immediately gives us an indication of how close the fracturing is to instability, and a study of the published diagrams in this light is quite interesting.

With regard to correlations between different kinds of tests, a great deal of work has been done, and today it must be accepted that there is no simple correlation, and it is not possible to establish 'conversion factors' to find equivalences even between such similar tests as the Izod and Charpy V-notch tests.

The point raised by Mr. Holliday about possible metallurgical changes during testing can, I think, be best understood by comparison with the behaviour of steel. For steel, Crussard *et al.*,† have shown that when impact energy is plotted against temperature, two curves are obtained; one for ductile (stable) behaviour and the other for brittle (unstable) behaviour, the former being above the latter. These two curves overlap, and there is a temperature range in which the choice of behaviour is a matter of chance (Fig. *B*).

In the aluminium tests, it appears that over the chosen range of test temperatures, the behaviour was always on the upper, or stable curve, and no jump over to the unstable behaviour was observed.

Mr. **C. I. Kelly** (Petroleum Consultant): All the published information available to me seems to indicate that steels have been the favoured materials used in the construction of large storage tanks for liquid methane, on a commercial scale. The following illustrations are examples that I have collected.

Figure *C* is an artist's impression of a pilot plant with a liquid methane storage in the form of a horizontal cylindrical tank 10 ft diameter, 18 ft long, able to contain 12,080 imperial gallons of liquid methane. Operated in Cornwell, Virginia, in 1939, it was made of 2% Ni alloy steel with 2 ft of formed cork round the outside, suitably waterproofed, and resting on wooden trestles. Satisfactory operation for four months provided design data for the larger plant shown in Fig. *D*.

This spherical tank, 57 ft diameter, stored 510,000 Imperial gallons of liquid methane. Three were built into the Cleveland plant of the East Ohio Gas Co., in 1941. They were made of 3½% Ni normalized steel with a standard carbon steel outer container for the insulation, which itself comprised 3 ft of formed cork in the lower portion and granular cork about it. The whole shell was suspended on legs to prevent any shock passing from the foundation to the inner, liquid-containing, metal sphere.

The next stage in the development took place in 1943, when the storage capacity for liquid (peak-load) methane was increased by constructing a vessel entirely different in shape and size. Figure *E* shows a toro-segmental tank 72 ft dia., 42 ft high, able to contain 925,000 Imperial gallons of liquid methane. Made of the same metals, this tank design was different in that the whole assembly was suspended on two circular H-section girders, one inner and one outer, separated from two circular concrete foundations by two circles of 30 and 60 wooden insulating pillars.

Further progress in this field was delayed for many years by an unfortunate disaster. In October 1944, the toro-segmental tank failed, its contents spilled and caught fire The third spherical tank failed under the effect of the heat and emptied its contents into the fire. The remaining two spherical vessels of 3½% Ni steel which had worked satisfactorily for 4½ years, not only survived the fire but were found to contain liquid methane after it had been put out. Figure *F* shows these from the street after the event.

Figure *G* shows another storage system, said to be operating near Moscow. The American design provided 100 vertical cylinders, 10 ft dia., 42 ft high, each able to contain 16,800 Imperial gallons of liquid methane. They were intended to be fabricated with 3½% Ni steel but I learned from Russian sources recently the steel actually used was an 18% Cr–9% Ni–1% Ti alloy.

With these examples I would ask the authors whether they have anything to offer which would convince us that aluminium alloys will work as well or for as long a time, as steels, on similar duties?

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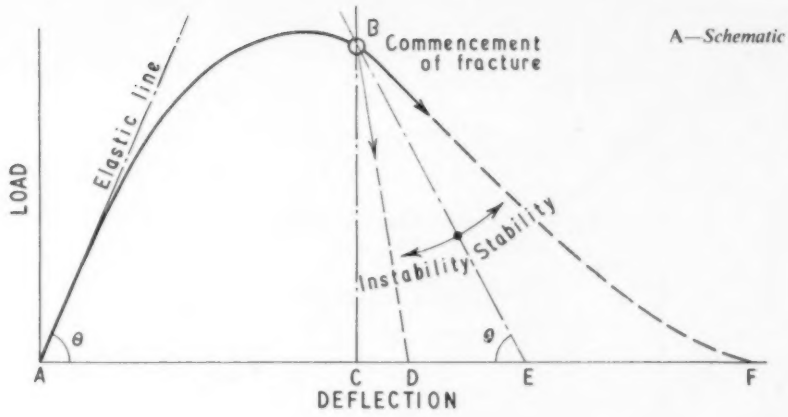
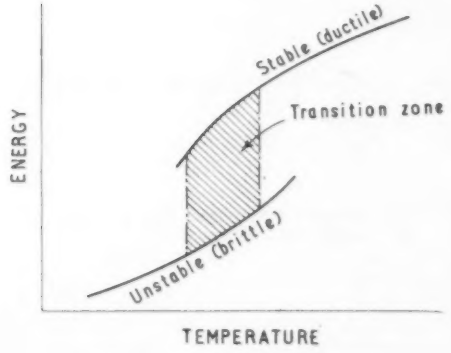
* G. M. Boyd, *Engineering*, 1953, 16th, 23rd, November.

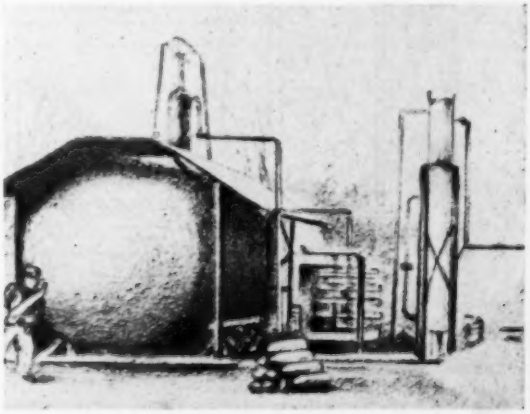
† C. Crussard *et al.*, *J. Iron Steel Inst.*, 1956, vol. 183, pp. 146–177.



A—*Schematic load/deflection diagram*



B—*Impact-energy/temperature curves*



C—*Artist's impression of Cornwell pilot plant*

Mr. **E. Flintham** (Quasi-Arc Ltd.): I want to draw attention to some apparent differences between the conclusions of Mr. Tomlinson and his co-author and Mr. Lismer.

Mr. Lismer seems to show that the maximum properties are developed in the range −125°C. to −150°C. as, for example, in his Figs. 28 and 29, and also in the conclusions on page 538 to which Mr. Birkhead referred.

But Mr. Tomlinson speaks of the significant figure being −78°C. as, for example, in Fig. 9, where he shows rather an abrupt change of properties from the maximum at about −78°C. There is a substantial difference between these temperatures, at least 50°, which might be important.

I am wondering how much the conclusion of Mr. Tomlinson and Mr. Jackson is based only on tests made with the use of solid carbon dioxide, giving a temperature of −78°C., and of tests made with the use of liquid nitrogen at −196°C., or whether there has been sufficient testing to build up a transition curve, such as has been done by Mr. Lismer.

Mr. **D. H. Tantam** (British Oxygen Research and Development Ltd.): My company has been using aluminium for the transport of liquid oxygen and nitrogen for over six years. During this time, we have used static road vehicles, portable vehicles, vacuum containers and the like. The vessels have been cylindrical, about 5 ft dia. and 9 ft long and made from 2½% Mg alloy. More recently we have had constructed to our own design a 10 ft dia. cylindrical vessel, in 1¼ in. thick 5–6% Mg alloy.

In the first place we carried out tests on the low temperature mechanical properties which convinced us that the materials would be satisfactory and as far as our own practical experience is concerned, nothing untoward has happened.

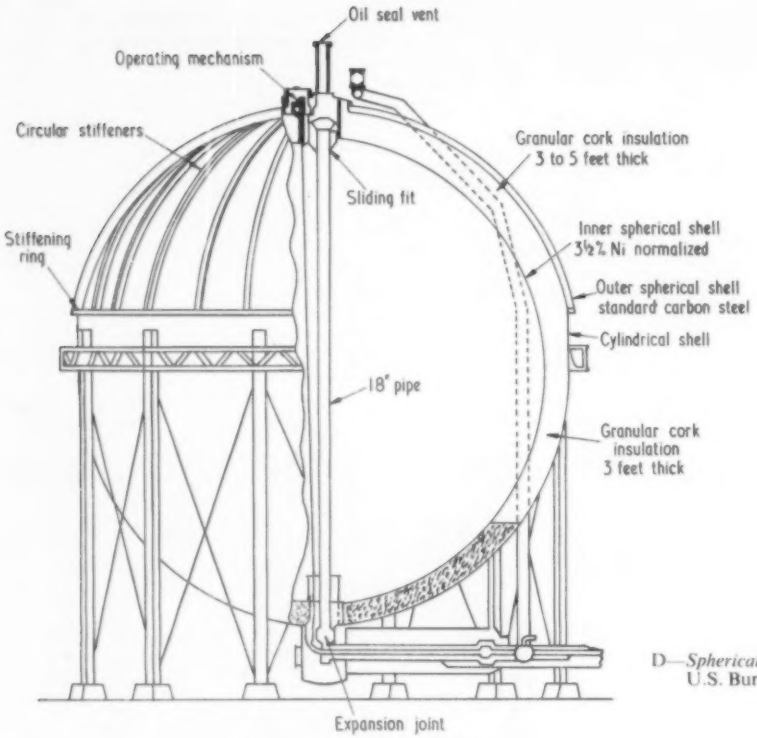
The results quoted by the authors are in themselves very helpful, but in practice one has to take account of the temperature gradient in the tank assemblies or its supports while under low temperature conditions. In some cases the permissible stress is limited by the recycling between ambient and operating temperatures rather than the loads imposed at low temperature.

Also one has to take account of the operational loads, thermal shock, and externally applied impact loads. More particularly one needs to investigate the use of heat-treatable alloys for use in flanges, joints and bolts; papers confine themselves—because the work was associated with a storage project—to non-heat-treatable alloys.

With regard to crack propagation at low temperature, one of our early tests may be of interest. This was on a prototype vessel which was stressed at low temperature until failure occurred; but the fracture did not propagate throughout the whole vessel.

Turning to the observations of a previous speaker it is quite true that mild steel can be used under controlled conditions at low temperature, and it will behave satisfactorily. It is probable that the vessels shown by Mr. Kelly were operated under such controlled conditions, but engineers tend to use high safety factors and would not generally be content to use steels where the operating conditions of the vessel were uncertain.

Aluminium and austenitic stainless steel, for instance, can be used for these vessels, and Mr. Kelly refers to 3½% Ni steels. All these, as far as the engineer is concerned, must



D—*Spherical liquid gas holder* (Courtesy U.S. Bureau of Mines)

be weighed on their merits. There are times when a stainless steel vessel, quite apart from cost, is a much better proposition than aluminium, depending on the stresses, strength, weld strength and so on.

I should like the authors, if they could, to say what thickness of aluminium they would consider, in a fabricated vessel, is the practical limit for use at low temperatures.

Mr. **W. C. Holliday** (Whessoe Ltd.): The important lesson that was learned from the earlier vessels constructed with alloy steels is how essential it is to test materials before the vessel is built. Mr. Kelly did not indicate that in the case of the spherical vessels that failed, the consequential damage was of the order of six million dollars, with loss of life of 128 people. That in itself is sufficient reason to test the materials before construction of such vessels which have to work under very difficult conditions.

The important conclusion drawn from the enquiry into the failure of 3½% Ni steel was that this material became unstable after long periods of service at low temperature. In fact, the impact value dropped from some 15 to about 5 ft lb Charpy keyhole. All enlightened people who have to construct these vessels look at the failures and draw considerable information from them. In fact, the more information there is available at the time of construction the safer is the vessel likely to be.

The 18/8 type steels, which Mr. Kelly said were being used for the construction of the storage tank in Moscow, do not show this instability at low temperature.

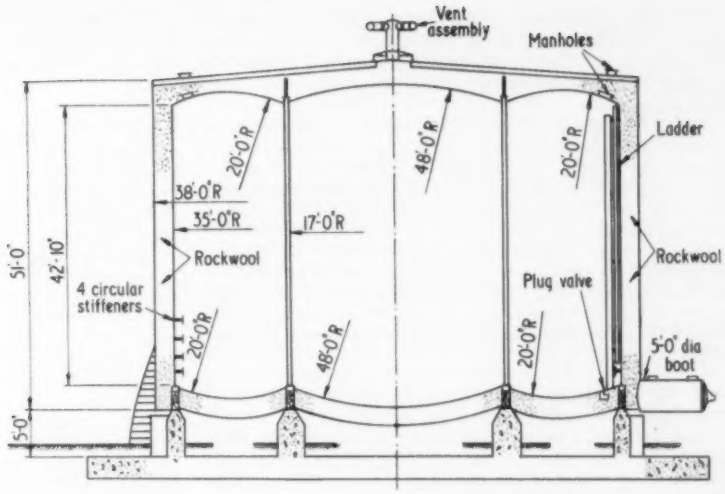
When my company first built a number of liquid nitrogen containers, there was no information on low temperature properties of 18/8, as was the case with fabricated storage tanks using aluminium alloys, and we had to do our own tests. It was found that, unlike alloy steels of 3½% Ni type 18/8 stainless steel did not have a marked transition temperature.

Our experience has been the same with the aluminium alloys which we have used for building tanks for storing liquid methane. With the exception of one aluminium company and Lloyd's, no one was doing any work on it at the time, and we did tests to make sure that in building this vessel we were not taking any unnecessary risks. At present, it is possibly cheaper to construct aluminium tanks than stainless steel tanks, although again this may depend on other factors.

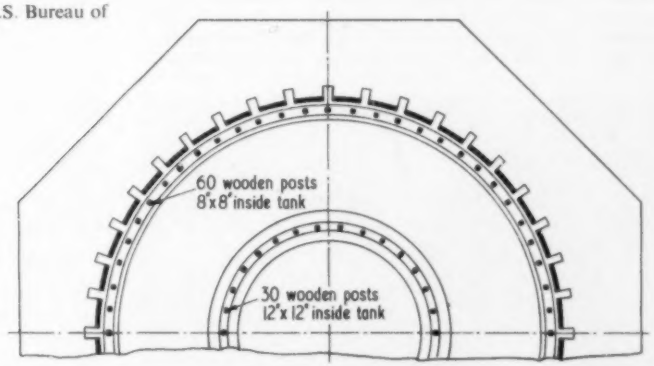
I have noticed that when specimens are tested at room temperature and at −70°C. the fracture front is fairly short. At −196°C. it goes right across the specimen. This is shown in most of the papers and on the tests we have done. The same sort of phenomenon occurs when steel specimens are tested, admittedly at higher temperatures. Can the authors give reasons for the different types of fractures?

Mr. **E. Elliott** (Aluminium Development Association): I support Dr. West's desire for further work and I should like to see investigated the stronger aluminium-magnesium-silicon-manganese alloy covered by the British Standard designation H.30. It might well be tested in the solution treated and fully heat-treated conditions and also welded.

As regards casting alloys, tests should be made of the aluminium-magnesium, aluminium-silicon and aluminium-silicon-magnesium types. In some unpublished work which I have seen on the properties of aluminium alloys at reduced temperatures (including a few specimens of casting alloys) it was concluded that cast test bars showed rather low impact values but it should be remembered that they also show rather low values at normal temperatures. It is very important in testing casting alloys to relate properties at reduced temperatures to those at normal temperatures. There will be some circumstances involving low temperatures where casting alloys will be useful and I suggest that they should be tested not only as-cast but also, where



E—*Toro-segmental liquid gas holder* (Courtesy U.S. Bureau of Mines)



HALF PLAN OF FOUNDATIONS

applicable, in the heat-treated condition and also welded perhaps to wrought products.

I was glad Dr. Sawkill drew attention to what seems a very reasonable explanation of the (at first sight) rather alarming fissures which appeared in some of the specimens. It is not really surprising that these alloys and indeed many materials rolled into plate or extruded into sections show rather lower properties in the short transverse than the longitudinal direction.

Beta precipitation is a popular subject nowadays but no one should be alarmed at Dr. Hargreaves' remarks. The aluminium-magnesium equilibrium diagram shows that aluminium does not dissolve much magnesium at room temperature so that under equilibrium conditions there will be a second phase. Alloys never are in equilibrium of course and with the lower contents of magnesium there is little beta phase present, but with the larger proportions of alloying elements it appears. Even then it has to be just in the right place if intercrystalline or stress corrosion is to occur, namely, a hard continuous line round each grain boundary.

Mr. G. M. Boyd: Mr. Holliday questioned possible metallurgical changes taking place during testing. Probably the easiest explanation is in terms of what Crussard found about impact tests on steels. There are really two impact curves, one for the ductile mode and the other for the brittle mode. As the temperature is lowered there comes a point at which either cleavage or intercrystalline fracture takes place, and there is a jump suddenly from one curve to the other. In practice, of course, we draw a single curve. But for aluminium alloys it appears that the curve runs all the way down from the top, and although there has been a slight reduction in the energy, reflected in the different type of fracture surface and in the load deflection diagrams, there is no sudden jump-over into the other kind of fracture, which is so perilous in the case of steel cleavage or intercrystalline fracture.

The Chairman (Dr. **Weck**): Listening to the papers and to the discussion, I have become increasingly more impressed by the complete absence of any reference to a subject which I feel quite sure would undoubtedly have dominated any similar discussion on steel; the question of the effect of residual stresses.

When brittle fractures in steel were first investigated, residual stresses were held entirely responsible for their occurrence. It was not the fact that steel was brittle at low temperatures that accounted for them. It was assumed that the residual stresses introduced by welding had embrittled the material.

We think we know now that this is not quite true, but we also know that residual stresses play an instrumental part in the brittle fracture of welded steel structures. The engineer is probably not concerned that the fractures are brittle but that they occurred at such extraordinarily low stresses, of a magnitude normally permitted in service and normally applied.

We also know that in the absence of such residual stresses it is only possible to produce the low-stress brittle fracture by applying loads that will produce a state of general yielding in the structure long before complete failure can occur.

No reference at all has been made to the effect of residual stresses on the behaviour of large vessels made in aluminium alloys, which obviously cannot be stress-relieved. This is one aspect which should not be completely neglected, for it is always possible that aluminium alloys will not behave like steels. Even if there is no embrittlement, there is always the possibility that the fracture strength of the material will be reduced by the presence of residual stresses. The question whether aluminium alloy pressure vessels destined to be used at low temperatures should be stress-relieved and at what temperature is a very important one.

Mr. J. C. Bailey (Aluminium Development Association): Some work in America has shown that if a component such as a casting or a heat-treated aluminium alloy forging or extrusion is reduced in temperature very considerably, and then brought back to room temperature, there is notably less distortion when the component is machined. This leads to the thought that the residual stresses have been redistributed, if not completely relieved. It may therefore be worth studying the properties of welded components incorporating residual stresses, by examining their properties before and after chilling to low sub-zero temperatures.

F—*Two intact spheric l vessels after fire* (Courtesy East Ohio Gas Company)

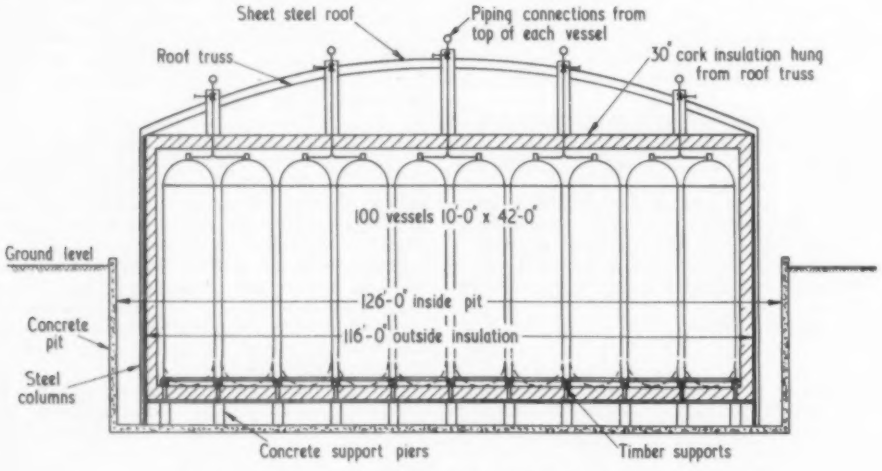
Authors' Replies

Mr. J. E. Tomlinson: Mr. Birkhead has asked us to elaborate on the relative behaviour of stainless steel and aluminium alloys at low temperatures, but as we carried out only a few tests on stainless steel, it is not possible to carry the comparison much further than we did in our paper. However, the work by Ball and Balajiva* generally confirms the results we obtained on the stabilized 18/8 steel: in particular that the rise in strength of these materials at low temperature is considerably greater than that for aluminium alloys. An examination of their results also shows that steels with the lowest proportional increase in tensile strength showed the smallest proportional decrease in reduction in area. The results of their Charpy V-notch impact tests showed that at both room temperature and —196°C., weld metal had an impact strength about one-third that of the parent plate, whereas, using Izod impact tests, we found that for Al–Mg alloys, both at room temperature and —196°C., the weld metal has an impact strength roughly two-thirds that of the parent plate.

We have found that the fissuring or 'pseudo lamination', which is apparent in low-temperature fractures in Al–Mg alloys, is reproduced in stainless steel with even greater intensity. In neither case would we regard this as a defect: it seems characteristic of ductile materials at low temperatures.

In discussing Mr. Lismer's paper, Dr. Hargreaves has noted that the results on NE. 5 might be regarded as the optimum for low-temperature work. The samples examined certainly require high crack propagation energies, but in view of the fact that alloy *B* (which has a magnesium content only 0·5% higher) requires much lower propagation energy, one might consider the importance of factors other than chemical composition. For example, the NE. 5 had an extruded structure and the crack would be running transverse to the direction of extrusion, whereas Mr. Lismer's alloy *B* would have a hot-rolled structure and there is no information on the direction in which the crack was made to propagate. We have found that propagation energies are structure-sensitive.

* F. A. Ball and K. Balajiva, British Commonwealth Welding Conference, London, June, 1957, pp. 113–118.



G—*Liquid methane storage, designed in U.S.A., for erection near Moscow, U.S.S.R.* (Courtesy, *Gas Age*)

Mr. Flintham has drawn a comparison between the positions of the maxima in Fig. 9 of our paper and Figs. 28 and 29 in Mr. Lismer's paper. It is quite possible that the maxima lie between the solid CO_2 temperature (−78°C.) and −196°C. The accuracy of the determination will depend on the closeness with which the test temperatures are spaced. Clearly the curve for HE. 9 in Fig. 29 of Mr. Lismer's paper will have a maximum within this range. Mr. Flintham refers to the graphs mentioned above as "transition curves", which we consider is not a good term for these materials, as they do not show the change from shear to cleavage fracture which is implicit in this term through its wide usage for ferrous materials.

Dr. Weck has drawn attention to the effect of residual stresses. We believe that this is especially serious in ferritic steels, because such stresses help crack initiation without significant plastic work, and which, if the temperature is low enough, is followed by immediate rapid propagation, principally by cleavage. In view of the large amount of plastic work needed with aluminium alloys before cracks can propagate, we doubt if the effect of residual stresses will be as important.

In this connection, it has often been suggested that the point of maximum load in a crack propagation test represents crack initiation and it is on this assumption that crack initiation and propagation energies are sometimes presented. The experiments we carried out with centrally-notched plates have shown that this is not the case, and at loads which are a relatively small proportion of the maxima attained, small cracks are formed which grow and rotate into the plane in which ultimately they will propagate up to the point at which the maximum load is attained.

Mr. R. J. Durham: I should like to thank all the contributors to the discussion. Several of them mentioned further work: H. 30 in particular was referred to.

We have done a few Navy tear tests on extruded H. 30 material and it behaved in a ductile manner requiring appreciable energy to propagate the crack. How it performs when it has been made by rolling I cannot say at the moment.

There are certain difficulties which can occur in testing: the characteristics of the testing machine which one uses to do Navy tear tests may have some bearing on the results obtained.

In tests on small specimens it sometimes happens that the test-piece breaks quickly, with little or no energy needed from an external source to propagate the crack, even though the reduction in thickness along the crack path may be sufficient to show that the crack is not brittle. The large amount of energy stored in the shackles and testing machine can sometimes exceed the expected propagating energy value, and the excess energy available at the fracture surface may be released at a greater rate with falling load than the energy release rate needed for fracture. It may be, therefore, that the Navy tear test could occasionally show a material to be more susceptible to cracking than it really is. Mr. Boyd, in his Fig. A, has distinguished between the graphs of stable and unstable behaviour. If, however, the crack runs sufficiently slowly across the plate elastic energy in excess of that shown might be available to propagate the crack.

Mr. Kelly has largely been answered by other contributors to the discussion. Steel may still be the favoured material, but it is hoped that work such as that reported will advance the use of aluminium for suitable applications.

Mr. Tantam asks about the thickness limit for aluminium alloys. Data illustrating the effect of thickness on notch behaviour are not yet available but we hope to obtain information relating to this point in the not too distant future.

Dr. J. Sawkill: In reply to Mr. Birkhead on correlating impact and tear tests, we have not attempted a correlation. As Mr. Boyd points out it is not even possible to establish conversion factors between different types of impact test.

We note with interest the comments on how the work might be extended. It is our intention to do low-temperature tear tests on other wrought alloys and on casting alloys.

Mr. R. E. Lismer: Unlike the other authors, I am not employed by a company selling aluminium alloys, but by a Society with the responsibility of approving the use of these alloys to operate under certain conditions of service and setting the maximum design stresses to be allowed. This has enlarged the scope of our testing programme and I hope to be shortly satisfying Dr. West's request for information on other metals.

Mr. Birkhead raised the point that, in general, in view of the optimum properties to be attained at −150°C., what further decrease could be expected below −196°C. and towards absolute zero? Dr. West has mentioned that pure aluminium continues to increase in tensile strength to four times its room temperature value at 4° absolute, and retains the same ductility. Other single-phase metals are anticipated to show similar features. But under notched conditions of testing, presence of secondary phases would cause some decrease in ductility.

Mr. Birkhead's request for more information on the low-temperature properties of weld deposits can be partially answered by the following results of tests carried out on butt-welded specimens prepared from two qualities of extruded flat bar aluminium alloys, manufactured to BS.1476, NE. 5 and HE. 9 specifications.

On each alloy, weld deposits were prepared using 16 s.w.g. filler wire of NG. 6 (Al–5% Mg) and NG. 21 (Al–5% Si) qualities. The plate edges were machined in symmetrical double-U contour and the plates pre-heated to 200°C. before welding. The welds were radiographically and macroscopically examined before being prepared for tests, and although some porosity was revealed, the amount was within the acceptable standard. No other defects were indicated. The quality of the weld deposits was regarded as satisfactory. The results of analysis, tear tests and notched tensile tests on the parent material, are contained in Tables I, II, and III respectively of my paper under the designations NE.5.M and HE.9.WP. The accompanying tables contain the following data:

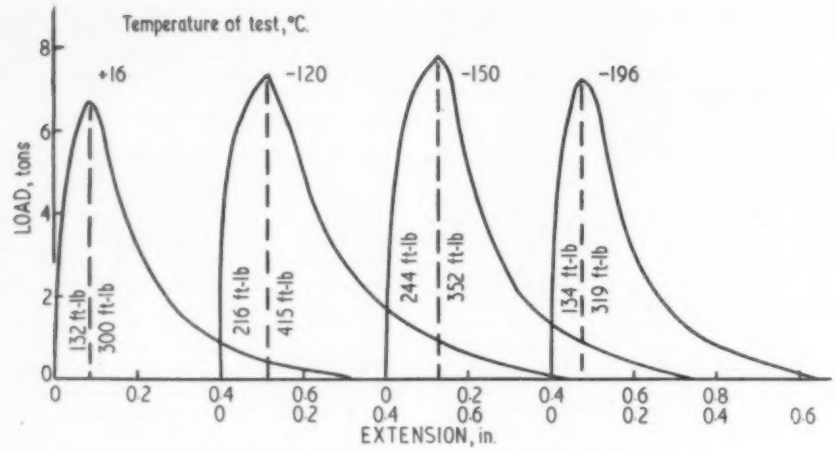
Table 1—Navy tear tests at ambient, −120°, −150° and −196°C. on specimens notched to enforce tear within the weld deposit. The load-extension curves are shown in Figs. *H–K*

Table 2—Transverse tensile tests at ambient and −150°C.

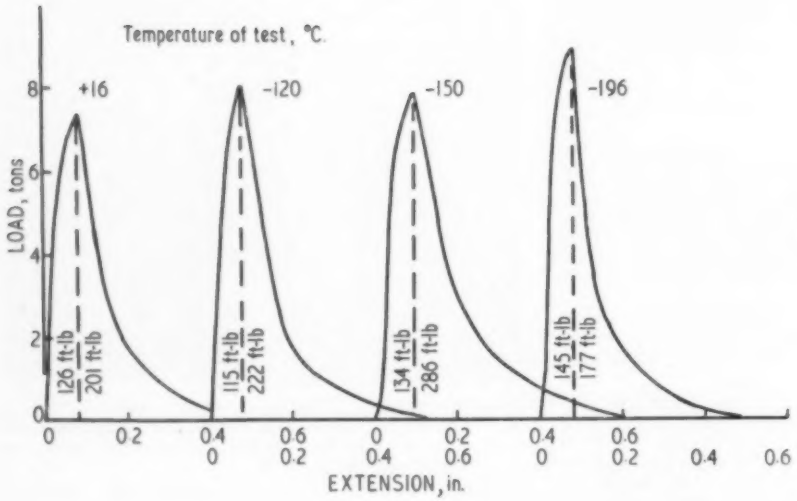
Table I

Results of Navy tear tests

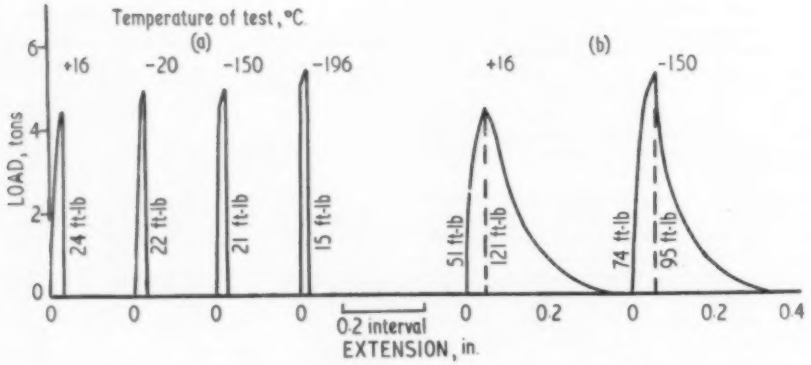
Parent Material	*Filler Rod*	*Temperature of Test, °C.*	*Maximum Load, tons*	*Corrected Energy Values ft.lb. Initiation*	*Propagation*	*Total*
NE. 5	Al–5% Mg	+16	6·65	132	300	432
		−120	7·3	216	415	631
		−150	7·7	244	352	596
		−196	7·2	134	319	453
HE. 9	Al–5% Mg	+16	7·4	126	201	327
		−120	8·0	115	222	337
		−150	7·8	134	286	420
		−196	8·9	145	177	322
NE. 5	Al–5% Si	+16	4·4	24	—	24
		−120	4·9	22	—	22
		−150	4·95	21	—	21
		−196	5·4	15	—	15
HE. 9	Al–5% Si	+16	4·45	51	121	172
		−150	5·3	74	95	169



H—*Load/extension curves for Navy tear tests with Al–5% Mg filler rod on NE. 5 alloy*



J—*Load/extension curves for Navy tear tests with Al–5% Mg filler rod on HE. 9 alloy*



K—*Load/extension curves for Navy tear tests with Al–5% Si filler rod on* (a) *NE. 5 and* (b) *HE. 9 alloys*

Table II

Results of tensile tests

Parent Material	*Filler Rod*	*Temperature of Test, °C.*	*Section, in.*	*Maximum Load, tons*	*Maximum Stress, tons/sq.in.*	*Elongation on 1½ in., %*	*Reduced Plate Thickness, in.*
NE. 5	Al–5% Mg	+16	0·710 × 0·990	10·7	15·22	Broke outside weld	
		−150	0·725 × 1·015	12·6	17·13	Broke outside weld	
HE. 9	Al–5% Mg	+16	0·605 × 1·000	6·35	10·50	Broke outside weld	
		−150	0·605 × 1·000	7·83	12·95	Broke outside weld	
NE. 5	Al–5% Si	+16	0·610 × 1·000	7·50	12·30	12% Broke in weld	0·570
		−150	0·600 × 1·000	8·25	13·75	12% Broke in weld	0·570

Table III

Results of notched tensile tests

Parent Material	*Filler Rod*	*Temperature of Test, °C.*	*Section at Notch, in.*	*Maximum Load, tons*	*Maximum Stress, tons/sq.in.*	*Elongation on 1½ in., %*	*Reduced Plate Thickness, in.*
NE. 5	Al–5% Mg	+16	0·725 × 1·200	11·25	12·93	9	0·600
		−150	0·720 × 1·220	12·9	14·68	9	0·610
HE. 9	Al–5% Mg	+16	0·600 × 1·220	9·05	12·36	8	0·530
		−150	0·605 × 1·380	12·25	14·67	6	0·560
NE. 5	Al–5% Si	+16	0·600 × 1·380	7·35	8·88	2	0·555
		−150	0·605 × 1·380	8·05	9·64	2	0·570
HE. 9	Al–5% Si	+16	0·610 × 0·785	4·20	8·77	10	0·595
		−150	0·610 × 0·765	4·72	10·01	10	0·590

Table 3—Tipper notched tensile tests at ambient and −150°C. on specimens notched within the weld deposit

Table 4—Charpy V-notched impact tests over a temperature range of 0° to −196°C., on specimens prepared with the notch in the weld metal and parallel to the plate thickness

The results of the tests are summarized as follows:

(1) The maximum loads to fracture the tear test specimens of both qualities are considerably lower than those of the parent metals. For the Al–5% Si filler wire deposits, the values are considered to be very low.

(2) The increase of maximum load as the temperature of testing decreased to −196°C. is only small.

(3) The energies to initiate and to propagate fracture in the Al–5% Mg filler wire deposits are higher between −120° and −150°C. than at ambient or −196°C., at which these values are similar. Dilution of the Mg content of the filler wire when deposited on HE.9 alloy caused some decrease in the energy values.

(4) The energy values for Al–5% Si filler wire deposits are very low, particularly with the welds on the NE.5 alloy.

(5) In the tensile test, the weld strength of the Al–5% Mg filler wire deposit exceeds that of the parent metal, whilst fracture occurred in the deposit from the Al–5% Si filler wire.

(6) The notched tensile tests revealed slightly lower maximum stress values of the Al–5% Mg deposits compared with the parent plate, and considerably lower values for the Al–5% Si deposits. With this deposit on NE.5 alloy the ductility values are very low.

(7) Decrease of testing temperature had very little influence on the results of Charpy V-notched impact tests, and the values obtained on the Al–5% Si deposits are very low throughout the range of temperature.

In general, the Charpy impact value for NP. 5/6 material in the rolled condition (alloy *B*) gradually decreases from approximately 30 ft.lb. at room temperature to 12–14 ft.lb. at −196°C. The characteristic changes in fracture with decrease in temperature are not discernible from the Charpy impact specimens.

Table IV

Results of Charpy V-notched impact tests

Temp. of Test, °C.	*Parent Material: Filler Rod:*	*NE. 5 Al–5% Mg*	*HE. 9 Al–5% Mg*	*NE. 5 Al–5% Mg*
		Energy Values, ft.lb		
0		25·5	17·5	4·5
−30		24	19	6
−60		22	19	4·5
−80		24·5	20	4
−100		22	19·5	4
−120		21·5	18	4·5
−150		22·5	16	4
−196		24	11·5	5

I am in agreement with the comparison made by Dr. Hargreaves between the properties of the hot rolled and the extruded products. The latter are superior and should be used with advantage for smaller section components and piping materials. The problems of corrosion and effects of beta-precipitation are being carefully watched in connection with the service of the tanks installed in the "*Methane Pioneer*", but it is known that the impurities present in the liquid methane that could give corrosion troubles, are of an extremely low order.

I am fortunate in having the co-operation and considerations of Mr. Boyd as a colleague, who is closely following the low-temperature work being undertaken at Crawley Laboratory, and I am also indebted to him for his contribution to the discussion and enlightenment of the fracture characteristics of these alloys.

Mr. Kelly's contribution and illustrations were very interesting and I am grateful to Mr. Holliday for supplying an up-to-date answer to his remarks. A disaster as described by Mr. Kelly is sufficient to cancel any further consideration of such low-alloy steels, and before entrusting any metal to such service conditions it is necessary that the maximum amount of information should be obtained from tests which to some degree emulate the nature of any

expected failure. And it was necessary to obtain as quickly as possible all such information on the aluminium alloys of the NP. 5/6 composition or similar. At that time, it had already been decided that technically, economically, and from the viewpoint of availability, these alloys should be the primary consideration for the construction of the liquid methane storage and transportation tanks. I do not wish to convince anyone that Al–Mg alloys are the only choice of materials for these conditions. But whatever the choice of metal for use at low temperature, the modes of fracture which could occur during service must be understood. The design of tanks to avoid dynamic stresses or stress concentrations is a separate study, and in some cases low-alloy steel may be suitable.

Mr. Flintham's remarks can be endorsed, for it is necessary that the temperature range should be adequately covered. He will notice that, for alloys *A* and *B* of my own paper, the temperature gap of −120° to −160°C. should be reduced so that the transition in the appearances of the fractures of the alloys can be studied in more detail.

I thank Mr. Tantam for the useful details of the experience of his company and for drawing our attention to some operational factors. Stresses due to recycling and operation temperature gradients had received consideration in the design of the liquid methane storage tanks and ancillary piping equipment.

I am pleased to inform Mr. Holliday that we are pursuing the study of the low-temperature properties of structural stainless steels. Further fracture characteristics have been revealed and it is hoped that in the near future, I may have the opportunity of writing a further paper with a more detailed study of the characteristics of the fractures already described.

Mr. Elliott also drew attention to the appearance of the fractures, and along with Dr. Sawkill he associates fissures with brittleness in the short transverse direction. The comparison of the properties in the longitudinal, transverse, and short transverse directions are given in both Dr. Sawkill's and my own papers, and are in good agreement. My own results contain tests at the lower temperature, and it will be noted that, even in the short transverse direction, strength and ductility are higher than at room temperature. No brittleness is indicated and I think that the phenomenon of fissuring is really associated with micro-constituents. The alloys with the lower Mg contents did not show the phenomenon and there is no reason to believe that their short transverse properties would not be the poorest of the three directions, and particularly with the extruded alloys, which again did not reveal the fissuring.

Mr. Elliott's explanation of the beta-precipitation is welcomed, but I do not think that Dr. Hargreave's remarks should be taken too lightly. Any cold-working of the higher Mg alloy during vessel construction, which could include damage and heavy handling effects, may have detrimental effects.

Finally, may I thank the Chairman, Dr. Weck, for his contribution and his warning, for the future may hold for us a catastrophic failure in these alloys. I think that, unlike the steel vessels, such fracture propagation is likely to be predominantly within the weld deposits.

Second Session

A Survey of the Present Position on the Economics of Welding,* *By A. G. Thompson*

Dynamics of Welding Production Management* *By D. M. Kerr*

Economics of Multi-Spotwelding* *By M. H. Lummus*

Structural Applications of Stud Welding* *By R. W. Taylor and Dr. J. C. Chapman*

The **Chairman** (Mr. **Gilbert Roberts**): In any shop welded fabrication, the wages cost is a measure of the efficiency of design methods and management, and in reducing labour costs we are not so much trying to save money as to promote efficiency. The lowest labour costs may be associated with the highest earnings by the men and good management is partly the art of ensuring that the highest skill is the most highly paid.

The feet of welding per ton is a very good criterion for welding design. In this connection it is important to remember that overwelding of stiffeners may increase costs without additional efficiency. Intermittent fillet welding has as big a bearing on economics as any other single feature.

Mr. Kerr showed a very interesting curve on the accuracy of fit-up. This, of course, applies to ship structures and for ordinary bridges and structural work I should say that the economic tolerance may well be much less, about 0·01 in. rather than his 0·07 in. That is the sort of accuracy attained by using a planing machine or an end milling machine, and where these are used for compression surfaces it is possible to avoid a lot of welding; it is certainly more economical to use planing or milling machines, where available, than to make up bad fits by welding. I have always adopted the principle that the standard of fit-up for welded work should not be less than that to which we are accustomed in riveting. It is too often thought that welding is a sort of putty which can be used to fill up gaps.

Mr. **L. D. Connell** (Johnson Matthey & Co. Ltd.): Mr Lummus states that more current is required with series welding than with direct welding. Heating is proportional to I^2R, so that the same current should be required, whether it is flowing through one or two spots. Possibly the more massive backings needed for series welding absorb slightly more current but generally a higher transformer tapping only is required to produce a greater voltage to give the same current.

In the direct welding arrangement I cannot see the advantage of a knife switch. The conventional method of using blocks of copper with hydraulic loading and a copper braid interface seems to be simple and easier to cool.

One of the important advantages of press welding, not stressed by Mr. Lummus, is that of quality. In gun welding this depends on the operator, but it is beyond his control in press welding. This means that a guaranteed consistency of weld can be produced, so that in press welding inspection can be an accurate control on quality.

The lessons to be learned from this paper are, firstly that market research is required to ascertain that the capital outlay is warranted by the number of components required; and secondly that the components must be designed for welding. The welding engineer should not be consulted after the design has been settled but his views should be sought at the earliest possible instant so that components are designed for efficient welding.

Plastics are now coming to the fore, and I should like to have Mr. Lummus's views about the future of multi-spot welding in the motor car industry, in comparison with plastic body construction which, we are led to believe, will be the construction of the future.

Mr. **E. Flintham** (Quasi-Arc Ltd.): I would like to emphasize Mr. Thompson's plea for the need for more

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* *Brit. Welding J.*, 1959, vol. 6, pp. 105–108; 116–124; 109–115; 97–104.

Table A

Conventional Electrodes

Arcing time ($3 \times 1\frac{1}{2}$ min)	= $4\frac{1}{2}$ min
Chip slag from crater; insert new electrode; move into position for welding ($3 \times \frac{1}{2}$ min)	= $1\frac{1}{2}$ min
Allowed time per job	= $1\frac{1}{2}$ min
Total time per job	= $7\frac{1}{2}$ min
Output	= 8 per h
Duty Cycle	= 60%

Table B

Iron Powder Electrodes

Arcing time ($2 \times 1\frac{1}{2}$ min)	= $2\frac{1}{2}$ min
Chip slag from crater; insert new electrode; move into position for welding ($2 \times \frac{1}{2}$ min)	= 1 min
Allowed time per job	= $1\frac{1}{2}$ min
Total time per job	= 5 min
Output	= 12 per hr
Duty Cycle	= 50%

Table C

Time for welding one foot length of $\frac{1}{4}$ in. fillet weld

Welding Position	*Electrode size*	*Number of runs*	*Current, amps*	*Time, min*
Flat	$\frac{5}{16}$ in.	1	400	3·6
Horizontal-Vertical	4 s.w.g.	3	270	5·6
Overhead	6 s.w.g.	3	200	8·2
Vertical	8 and 6 s.w.g.	2	160, 180	8·8

Table D

Time to deposit 1 cu.in. of weld metal

Electrode size, s.w.g.	*Time, min*
4	2·65
6	4·00
8	5·35
10	7·75
12	11·30

investigation into economics and productivity in the use of welding. Too often, far too much emphasis is placed on securing electrodes at a cheaper price as if that were the only significant factor affecting the economics of welding.

For the theoretical method of estimating costs, the arcing time is often assessed from the quantity of electrodes used by all the welders at a works in a given period, say a week, from which an average percentage duty cycle per welder is obtained. This percentage is usually much lower than is anticipated and serves to emphasize that the non-welding time is the most costly part of arc welding fabrication. But, it is misleading to assume that the duty cycle remains fairly constant, or that a decrease in the arcing time implies decreased productivity, because, as Mr. Thompson so rightly points out, the elements in cost measurement are variable and often have unexpected effects. By changing from the use of conventional electrodes to iron powder types an increase in production may be obtained, but this is not necessarily associated with an increase in the arcing time.

In the example shown in Tables *A* and *B* a 50% increase in output was obtained by the change to iron powder electrodes, though the arcing time fell from 60 to 50%. This was for a mass-production job, using conventional electrodes, as detailed in Table *A*. By using iron powder electrodes, which deposit 50% more weld metal per electrode and also have faster rates of deposition because it is possible to use higher welding currents, the situation changed to that shown in Table *B*.

It is because of these unexpected effects that I suggest that for comparison of the value of two or more welding procedures, the method of "Work Measurement" is likely to be the more satisfactory. "Total Production Analysis" is more likely to be effective as a method of broad cost control for budgetary purposes.

Mr. Kerr has referred but briefly to the comparative welding speeds for various welding positions. Possibly this is because in shipbuilding one has, to a great extent, no choice in deciding the welding position. However, where choice is possible, it has a significant effect on welding costs. The example shown in Table *C* giving times for welding one foot of $\frac{1}{4}$-in. fillet weld will serve to supplement the information given by Fig. 7 in the paper. This clearly shows that the speed of welding is related directly to the size of electrode and the amount of current that can be used in the various welding positions.

The possibilities for using larger sizes of electrodes when welding in any position are often overlooked, but Table *D* shows how significant is this aspect.

Much could be done to improve the standard and reduce the cost of manual metal-arc welding if every welding shop manager would ensure that the advantages to be gained by positioning fabrications so that they can be welded in the flat position and the possibility of using a larger size of electrode for any particular weld are not overlooked.

Mr. **H. Martin** (Consultant): As one of the main originators of studwelding in the period 1918/19, I am extremely gratified to see these developments which are now taking place all over the world.

In structural steel work, one cannot turn the structure upside down to make it convenient for horizontal surface studwelding. It would be interesting to know the extent to which the exploiters and developers have encountered demands for steel studwelding out of the vertical and whether they have experienced any serious difficulties.

When welding on vertical surfaces, the ferrule gives a considerable advantage, but there might be cases in which a special type of ferrule would advantageously be employed. In overhead work, there seems to be no serious difficulties though rather higher current densities and shorter weld times are needed to avoid the danger of molten metal dropping out. It would be of interest if Mr. Taylor could give us a few remarks about that.

I presume that clearance holes in roof sheets are needed to provide space for the ferrule. I should point out however that under certain conditions and with some equipment it would be possible to get welds of quite good quality without the use of the ferrule.

Mr. **E. H. Lee** (Metropolitan-Vickers Electrical Co. Ltd.): In dealing with fitting inaccuracies and their effect on fabrication costs, Mr. Kerr refers to gaps of up to $\frac{5}{16}$ in. in fillet welded joints but I should say that something like $\frac{1}{16}$ in. or, in certain circumstances and in certain types of fabrication, perhaps $\frac{1}{8}$ in. should be the maximum. This reference to gaps which are greater than those acceptable in good practice helps Mr. Kerr to emphasize his point but it gives a misleading impression.

In regard to distortion, I believe he has oversimplified the comparison between manual and automatic welding, for the empirical formula $D = A + N + C$ takes no account of welding current and travel speed. Is it possible to produce an empirical formula which would take care of these other factors?

In discussing design for welding, Mr. Kerr says that designs which introduce butt joints and straight lines throughout large plated structures are regarded with apprehension in certain structural circles. Why is this so and why do such designs require changes of methods and, apparently, closer supervisory control of the plate preparation and welding?

Mr. **L. H. Morgan** (Quasi-Arc Ltd.): I congratulate Mr. Kerr on a valuable contribution to the subject, because there is no doubt that bad preparation affects production costs very considerably. I particularly like the graph showing declining welding costs and increasing preparation costs with a break even point, a conception normally used by Sales Management, but nevertheless usefully applied here.

I do not agree with Mr. Kerr when he speaks in terms of plate accuracy of 0·015 in. My experience is that a tolerance of twice this amount is more realistic, although frequently we are asked to automatically weld butt joints with a tolerance of as much as $\frac{1}{8}$ in. One interesting feature about preparation standards is that whilst it is often an uphill struggle to get what you want initially, the problem virtually disappears with the passage of time. This has sometimes happened in my experience, without any new equipment being installed for edge preparation, so that without doubt good preparation depends in part on experience and attitude of mind.

In addition to the unnecessarily high welding costs brought about by excessive gaps, considerable savings can also be made by the use of flatter butt and fillet welds. Specially designed manual electrodes now available give a mitre shaped h.v. fillet with consequent longer run lengths compared with the conventional convex profile. Another new electrode for the vertical-down welding of butts and fillets is now available, which shows even greater economies and has been most successfully adopted for the welding of oil storage tank vertical joints.

In automatic welding, a smaller fillet weld than is required by manual welding can often be used because of the greater penetration of the former. As an example, a $\frac{1}{4}$ in. automatic fillet weld is almost equal to a $\frac{5}{16}$ in. manual weld, and this procedure is now accepted by Lloyd's. Such savings of weld metal and operator time can make very appreciable reductions in welding costs.

I should also like to touch on the question of duty cycle, because since welders spend more time doing things other than welding than the arcing time itself, Management can make the greatest improvements in productivity here. Whilst Mr. Kerr has dealt with some of the problems of organization and control which affect duty cycle, I would suggest that insufficient attention has been given in the past to the welder himself, and the motivating force which encourages him to improve his performance.

The modern welder, particularly those working on site to high radiographic standards as required in shipbuilding, and on oil and atomic energy sites, requires a combination of mental and physical ability which is rapidly becoming more exacting than almost any other trade. As a result, adverse working conditions can bring about serious losses in production through a decline in morale and the will to work.

There are for instance the physical aspects, such as static fatigue and discomfort caused by bad positioning of man or job, temperature conditions, humidity, ventilation, bad lighting and excessive noise. There are also the psychological aspects such as speed stress, load stress, anxiety due to high individual responsibility, monotony and even loneliness. These less tangible aspects will require the attention of dynamic management in the future, for whilst the rate at which weld metal is deposited depends mainly on amperage used and can easily be calculated, the problems I have mentioned respond to no such simple law.

Mr. **P. Shaw** (Quasi-Arc Ltd.): The efficient use of floor space is just as important as the efficient use of plant but it is an aspect of productivity and costing which is to some extent overlooked, especially in erection and fabrication shops.

I wonder what the costs for some fabrications would be if, instead of applying all overheads on the basis of the many hours worked on the job, at least a proportion were to be based on the number of hours that pieces of work stood on the floor—in other words, per square foot of floor area occupied. This would create an incentive to see that work is put through the shop quickly, because the increased costs occasioned by slow throughput do not appear on the cost sheet.

I wonder if anyone has considered taking a photograph, say every hour, of a certain section of his fabrication shop, automatically by mounted camera and then examining those photographs over a period and seeing what changes have taken place? I suspect that if such a technique was used it would be found that the time that work lies on the floor because somebody was waiting for a crane to lift an automatic welding machine from one place to another, was much higher than any of us imagined.

Mr. **M. Seaman** (British Oxygen Gases Ltd.): In the field of management problems, new tools are becoming rapidly available in data processing equipment. This is also being applied to components and, indeed, to assembly production.

It is therefore a sound proposition to suggest that this is a subject which could well merit rational research by the Institute of Welding at this time.

One virtue of the numerical data processing system, with the related mathematics and the production analysis, is that one can go from the designer's mind, from the tank model, or the ideal empirically determined shape, straight through to the cutting of the shapes when computer-controlled gas cutting machines are used. The researches suggest that one can keep to the mathematical statements of shapes and of forms and positions. Designing techniques can then be specifically limited to take in the interests of manufacturing and functioning. The process can become a truly logical one to which the instrument of data processing, using computers and curve generators, can be applied.

It seems that Mr. Kerr has concentrated about five years of research into his paper. This now needs to be accelerated so that industry can be prepared to accept the new techniques available.

The **Chairman** (Mr. **Gilbert Roberts**): In a forthcoming contract which will involve a large amount of site welding, the contractor will not require more than about 300 amp; so he uses, perhaps, 400 amp transformers. There are also many studs to be welded to the steelwork for the anchoring of the concrete slab to it. What would be the most economic size of stud to use, if possible with the same welding sets that the contractor would normally use for the arc welding? That has a very big effect, of course, on the economics of the stud welding vis-a-vis the welding-on cleats and T sections, which are quite effective as an alternative form of connection of the concrete to the girders.

As far as I know, it is a universal practice to allocate overheads exclusively to shop wages, but the possibility of allocating part of the cost to floor space is very interesting. Such a method would show up some jobs which are suffering from 'suspended animation' which in any case are always the most costly jobs in the end.

Mr. Shaw can rest assured that the job just lying on the shop floor week after week is nevertheless still showing a mounting cost in the books, because the job number is marked on it and anyone who is doing nothing in particular can allocate his time to that job number!

Authors' Replies

Mr. A. G. Thompson: I find it difficult to confine myself to my own subject and not to encroach on Mr. Kerr's paper, because much of his subject also occupies a fair amount of my thought and time. Therefore, in regard to Mr. Shaw's remarks about taking hourly shop photographs, I think there would probably be labour trouble in some places if this were attempted. But the point is good and is sometimes not fully regarded.

The solution is often to plan these fabrication floor spaces ahead and to schedule them week by week or, if possible, day by day and to have man hours allocated to job numbers day by day. Then, it is possible to know how many people are working on each job every day. The space is pre-planned, it is known when a job is scheduled to finish, and those schedules will be kept if care has been taken to pre-plan the plate preparation, the stocking of the steel in the stockyard, and also the ordering and the design. The pre-planning of flow space is the end of a long chain of events and all the other links must be put into place first.

I thank Mr. Flintham for his kind remarks and his support on the statistical side. This links up to some extent with Mr. Seaman's comment that productivity has two components. In my paper it was inferred that productivity must be combined with performance in a product. These are the two components—performance and productivity.

A great deal of money has been spent on improving performance whereas there has been some tendency to neglect productivity. The British Welding Research Association, with an income now of £150,000–£200,000 a year, examines performance and the effect of all the different variables on performance. An equally important subject for research is productivity and the effect of all the many components which affect it. What are required are many graphs relating productivity indices and important variables. Once these graphs are available, computers and data processing can be used to optimize and maximize.

Data processing possibilities are such that complicated data and involved calculations can be dealt with in minutes instead of days. Such calculations can be used to influence and control production hour by hour. Here is a wonderful dream, but at the moment the basic data are missing. There are many years of research work to be done in examining these different factors.

Apart from these cost aspects, there is the question of efficient design for production; for instance, to reduce and take account of distortion. Until the recent translation of a Russian book, little fundamental work has been available.

What is needed—and one or two manufacturers are doing this—is to have a number of interested technical people taking measurements and attempting to relate them firstly, perhaps, to the Russian theory, and modifying this theory as necessary so that the designer, once he has worked out his stresses and strains, can also work out his distortions.

I would like to raise the question of inter-firm productivity comparisons. About two years ago, at an international meeting in Vienna on inter-firm productivity comparison, it was possible to see what activity was going on in the world on this subject, *i.e.*, comparisons between firms. In Germany, in particular, and in France and Scandinavia, national organizations are set up to do this. There is every opportunity to do it in our fabrication industry. The methods and foundations for doing this work have already demonstrated the possibilities.

This has been referred to in my paper, and it is suggested that the Institute might look at it and see what is being done in other industries and consider whether some action could be taken. The Institute of Welding stimulated and set the British Welding Research Association on the road. Could it not stimulate and set on the road an organization for looking at productivity in its broadest aspects, *e.g.*, comparisons, measurements, and so on? I wonder whether it might be possible, through the medium of this meeting to have the Council give this matter its consideration.

Mr. D. M. Kerr: Several speakers have referred to costs. Mr. Roberts stated that wage costs were an indication of efficiency. This is quite true. High wage costs with high output are generally associated with economical overheads and efficient management.

Mr. Hall raised the question of overhead costs. It is wrong to base the economics of overhead costs in relation to labour costs alone. It is possible for two concerns X and Y to market the same product at different prices. In X concern the overheads are low compared with their labour costs, but the latter may be uneconomically high. Whereas in Y concern the overheads may be high compared with labour costs which are low compared with those of X concern, and the combined overheads and labour costs of Y may be appreciably lower than that of X.

Mr. Hall's query reverts to the point raised by Mr. Thompson in his presentation: "*Where is the break-even point between overheads and labour costs?*" The answer depends on factors such as the general level of costs within the particular industry, the state of trade and the market, whether major development is in progress or completed within the concern, and many other conditions.

The criterion is the total cost of the completed product. A progressive check on costs during manufacture should indicate which costs are high and where action should be taken.

I agree with Mr. Lee's point about inaccurate fit in joints to be welded, but I consider that the data provided do indicate where latent welding costs may exist. He comments on the comparison in distortion between hand and automatic welding. It is possible that the empirical formula submitted may appear to be oversimplified, but I believe this is the best way to approach the problem in the early stages. It gives a quick and reasonably good indication of what is likely to happen with a particular joint design and welding procedure. It may be considered in the same manner as "The Phase Rule" in chemistry by Willard Gibbs. If one wishes to resolve the problem to absolute finality in the design stage, one is likely to be confronted with rather abstruse and complicated mathematics. The recent Russian work by N. O. Okerblom confirms this.

Regarding Mr. Morgan's comments on tolerances of up to $\frac{1}{8}$ in. being overcome in automatic welding, I do not agree with this and regard such tolerance as highly impracticable and very uneconomical. My view is supported by Mr. Roberts who has had considerable experience in the application of automatic welding to structural steelwork. In his opening remarks he stated his preference for 0·01 in. tolerance and that too often it is thought that welding is a sort of putty which can be used to fill up gaps.

Mr. Flintham's contribution is appreciated. I join in his support of Mr. Thompson's plea for more investigation into the economics and productivity in the use of welding. I am in complete agreement with Mr. Shaw regarding floor space productivity and with Mr. Seaman on the trend of development in automatic flame cutting.

I can vouch for the speed and efficiency of stud welding as discussed by Mr. Taylor. Our work covers the welding of thousands of $\frac{7}{16}$ in. deck planking studs and insulation clip pins. One or two stud welding operators working efficiently can keep quite a number of ships carpenters and insulators fully employed in fitting their respective fixtures after the studs have been welded.

Mr. M. H. Lummus: Replying to Mr. Connell it is definitely the effect of the current which gives enough heat to make a weld. The heating effect of the flow of electric current is proportional to C^2RT where C^2 is the most important factor, since one is trying to raise the component metal at the interface to its molten state. When two

welds are made with one secondary on a transformer, obviously more heat is necessary than when one weld is made, as in the case of direct welding.

The mention of voltage is important and relevant, since the short-circuit secondary current increases with d.c. secondary voltage. The transformers themselves have tappings on the primary side and by varying the setting the secondary current is altered.

The type of knife switch shown in my diagram has been used. It is not the best type, but it is much easier to show on a diagram.

Mr. Connell suggested a contact pad coming down, hydraulically operated or spring loaded, as a method of completing the circuit. But if there is a table coming up in vertical plane, the horizontal surface lends itself to the accumulation of weld splatter, grease, and oil and there is not quite such a good contact. The much more favoured condition is with the vertical contact face. An improved version of the knife switch shown is to have a circular one segmented, thus providing contact all the way round.

Mr. Connell also mentioned weld quality relative to press welding. In this process, one guarantees the size and shape of the weld far more than with a conventional spot welding machine or a portable welding gun. The operator is completely divorced from the making of the weld, and that is why, in the paper, I described the weld as a mechanical weld. With conventional spot welding, the components can be at an angle to the electrodes, which gives a variation in weld size and quality.

Another feature of multi-spot welding that has an effect upon weld quality is the way in which the electrode tips can be dressed. Since they always hit the panel at the same angle, the wear on electrode tips is reduced and mechanical dressers can be used to much greater advantage, whereas with conventional spot welders it is customary in most places for the operator to use a file.

Another of Mr. Connell's comments related to the use of plastics, and he asked what would happen with multi-spot welding. Plastics are very much in their infancy and their use for motor car bodies is a "long-winded" process. It will be a long time before we, as welding engineers, have to worry about sheet metal welding being superseded by the welding of plastics.

Plastics used with fibre glass in that way, are adhesives, and we welders do not like to think that we stick things together. In the event of plastics superseding sheet metal to any great extent, the only thing to do is to get the B.W.R.A. to carry out research on the welding of plastic materials to a much wider extent than at present. That would complicate matters for us as welding engineers, for we would then have to be more chemical engineers than the metallurgists that we are now.

Mr. Morgan was somewhat perturbed at the operating conditions, mainly on stud welding. We are probably in a far better position than the people on construction work to pay attention to operating conditions, because most of our operations are repetitive. In designing press-weld fixtures and also in laying down specifications for the machine, our operating conditions are borne in mind to a very large extent, so much so that the height of the table of the press welder is maintained at approx. 40 in., for easy loading of parts. The table then rises to about 52 in. or perhaps higher to weld, so that there is a good lift for ease of mechanical handling, which is another feature affecting production. We produce a drawing showing the operator in his working conditions and in his extreme positions; *i.e.*, leaning in to a machine to load, or leaning out of a machine, and we try to pay attention to his operating conditions and his safety.

Floor space has also been mentioned in connection with the other papers. Consideration is given to that in multi-spot welding. In Table I of my paper I listed floor space saving. It was left purposely as a comparison of area saving because floor space in monetary values varies extensively from site to site and from factory to factory.

Mr. R. W. Taylor: I do not know in what sense Mr. Martin meant that America might be ahead of us in Britain. If size of the market is the criterion the Americans are certainly ahead. Similarly, they may be advanced in developing new outlets and markets for stud welding. This is, I think, because in Britain we tend to be more conservative both in testing new processes and subsequently in adopting them when tests prove satisfactory. In other respects, however, I do not think America is ahead of us, certainly not in 'know-how' on stud welding.

In the 37 years since stud welding was first used there is no record, so far as I know, of studs larger than $1\frac{1}{4}$ in. dia. being welded by this process and I believe the largest so far welded on a commercial scale are 1 or $1\frac{1}{8}$ in. dia.

In regard to positional welding, for practical purposes and using conventional power source, $\frac{5}{8}$ in. dia. may be taken as the limit for welding on a vertical surface. Satisfactory welding to attachments up to $\frac{3}{4}$ in. dia. is, however, possible in the overhead position.

Stud welding without ferrules is possible, but the greater the care exercised in controlling surrounding conditions, the higher is the quality and consistency of welding. There is no doubt that the use of a ferrule helps considerably in this respect.

Operator fatigue while stud welding on site work, to which Mr. Morgan referred, has been given more attention in America, where stud welding of larger attachments is more common. For instance, to reduce operator fatigue to a minimum during the stud welding of $1\frac{1}{4}$ lb. shear connectors on site at speeds approaching 200 per hour, a labourer is used to keep stud and ferrule racks replenished. The use of these racks enables operators to re-load their tool with a stud and ferrule without actually handling either.

The Chairman enquired about the economic size of studs which could be satisfactorily stud welded using standard 300 or 400-amp arc welding sets on site. Most stud welding, whether in the shop or on site, employs direct current. The average 300 amp set will generally deliver sufficient current for a good $\frac{3}{8}$ in. dia. stud weld; most 400 amp machines are sufficient for a $\frac{1}{2}$ in. dia. stud, and a 600 amp machine will enable a $\frac{5}{8}$ in. stud to be welded satisfactorily.

Since the optimum stud welding current for a given attachment is proportional to its cross sectional area, it is possible to predetermine both time and current values for any given size of attachment from curves available.

Symposium on
FATIGUE OF WELDED STRUCTURES

The British Welding Research Association and the Engineering Department of Cambridge University have jointly organized a symposium on fatigue of welded structures, which is to be held in the Engineering Department of the University from 29th March to 1st April 1960. Seven of the papers to be presented and discussed are published in the following pages. The remainder will be printed in the April issue of B.W.J.

It is anticipated that a report of the discussions at the Symposium will be available in time for publication in the June issue of B.W.J.

Foreword

By R. Weck, PH.D., M.I.C.E., M.I.MECH.E.

THE effective dissemination of knowledge gained from research is a problem that in many ways is as challenging as some of the intricacies of research itself. It lies at the very roots of applied research, in welding no less than in any other field of technology. In fact, it is often seen that the lines of communication in welding are more diverse and tenuous. It is natural then that the British Welding Research Association should constantly be seeking to secure better dissemination of the results of its work. And it is a corollary of this that opportunities should be created from time to time for the work to be critically assessed by prospective users.

To implement these ideas the Council and Research Board of the Association have decided, among other things, to organize whenever appropriate special conferences and symposia, of which the Symposium on Fatigue of Welded Structures is the first.

The fatigue behaviour of welded structures has never before been the single subject of a conference, although as a subject for research it is virtually of fifty years' standing. However, it is more recent history rather than any desire to mark a jubilee that warrants its elevation to conference level. Research has been intensified in the last twenty years and the last decade has produced increasing evidence of the importance of fatigue strength as a criterion of structural strength.

The development of research in this field is not something with which the Association alone has been connected. Much important work has been done in other countries and valuable contributions made by other organizations in the United Kingdom. It is fitting, therefore, that a first symposium on the fatigue of welded structures should be planned to include contributed papers from abroad and from a number of sources in the United Kingdom, and should have in its membership acknowledged experts from a number of countries. It is, too, a matter of particular satisfaction that the Association has received the full co-operation of Professor J. F. Baker, Head of the Department of Engineering, Cambridge University, in the organization and accommodation of the Symposium. Any discussion of welded structures that is based on Cambridge must start with undeniably good prospects.

Eleven papers are to be presented. These will deal with structural elements and joints that are fabricated from steels and aluminium alloys by fusion welding. The limits here are more apparent than real, and there cannot be exclusive coverage even of this part of the subject. In the main the emphasis will be on 'structure' in the commonly accepted engineering sense, with the further qualification that loading conditions will normally be those involving large numbers of loading cycles. High-strain and high-temperature fatigue problems are not specifically discussed. Mild steel construction figures prominently, and the papers by Newman, Munse and Stallmeyer, Gurney, and Neumann collectively make a detailed review of present-day knowledge of this area of the problem. Three new lines of research presently related to mild steel are introduced in the papers by Gurney, Gilde, and by Whitman and Alder. The first and second of these suggest that useful improvements in the fatigue strength of welded joints can be obtained by relatively simple, but quite different means, and each will undoubtedly be scrutinized critically. Whitman and Alder describe the early stages of an investigation of the real assessment of service life, a problem that is clearly worthy of full discussion in a forward-looking conference.

The fact that high-tensile steels rarely exhibit fatigue properties better than those of mild steel when tested in the form of a welded joint is universally recognized, but since the economic rewards could be significant, research into this problem is actively pursued. The paper by Stallmeyer and Munse provides a most valuable opportunity of learning of the work being done at Illinois University. The fatigue behaviour of welded aluminium alloys is also a subject that is rapidly growing in technological importance, largely as a result of the very successful development of fabrication techniques for these materials. Tomlinson and Wood discuss a number of factors that influence the fatigue behaviour of aluminium alloys in a paper which serves most usefully as a general account of present knowledge, and Gunn and McLester describe recent research on the effect of mean stress on the fatigue strength of butt welds in these alloys. This research will be recognized by many readers as the extension of a critical study of mean stress effects, now brought into the welding field.

Finally, the papers by Biggs and by Lewis are devoted to future research. This matter will certainly have been referred to in other papers, but it was considered appropriate that the closing session should give free reign to any thoughts that had been stimulated. Guarding against the contingency that further stimulus would be necessary, the Association invited and was gratified to receive, the co-operation of two experts, a metallurgist and an engineer, both of whom are widely known in the welding world as unconventional thinkers. Their views on metallurgical and engineering aspects of future research will not be read without profit.

Dr. Weck is Director of Research of the British Welding Research Association.

SYMPOSIUM ON FATIGUE OF WELDED STRUCTURES

A Systematization of Fatigue Strength Values of Mild Steel Welds

Based on a quantitative formulation of Stüssi's fatigue strength theory, a systematization of the fatigue strength values of all types of welded joints in mild steel has been developed. A great many test series were evaluated with the aim of establishing fundamentals for calculating dynamically loaded welded joints.

By Dipl.-Ing. A. Neumann

In recent years Stüssi has established a general "theory of fatigue strength of materials and notched bars".[1] Its aim has been to find a *quantitative* formulation of the problems of fatigue strength, since the mathematical inter-relationship laid down by Wöhler represented only a *qualitative* formulation. Stüssi's theory evaluated Wöhler's basic principles as well as other test results obtained.

Based on this theory, an attempt has been made in this experimental work to establish a systematization of fatigue strength values of mild steel welds. The object is to compare all hitherto reported fatigue test results of equal or similar welded joints and to determine minimum values for practical applications.

Theory of Fatigue Strength

Generally, rough relations between the values tensile strength (f_B), fatigue strength under cycles of pulsating stress (f_{sch}), and fatigue strength under cycles of reversed or alternating stress (f_W), are known.

It is possible to give a graphical representation of some values on approximate fatigue diagrams.

Stüssi develops a mathematical relationship between fatigue strength values, utilizing fatigue strength curves according to Haigh (f_A—f_m diagram). Introducing certain conditions, Stüssi deduces the following relation between fatigue strength under pulsating and under reversed stresses:

$$f_A = f_w \frac{f_B (f_B - f_m)}{f_B (f_B - f_m) + f_w \cdot f_m}$$

where:

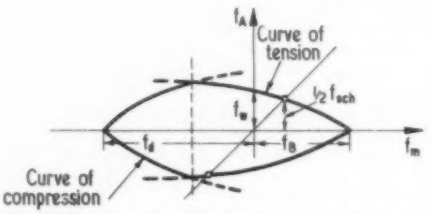
f_A = amplitude
f_m = mean stress.

This equation enables the positive tension values as well as the negative compression values of fatigue strength to be derived. Thus a complete fatigue strength diagram can be obtained, permitting the entire range of fatigue strengths to be read off the graph (Fig. 1).

Paper to be presented at a Symposium on Fatigue of Welded Structures, to be held at Cambridge University on 29th March–1st April, 1960.

Mr. Neumann is Chief Engineer and Director of the Research and Development Department of the Central Institute of Welding of the DDR, Halle/S.

It is well known that the notch effect has a marked influence on fatigue strength. This is particularly evident in welded joints, where notches can be seen to



1—*Graphical representation (Haigh's system) of the entire range of fatigue strength (Stüssi)*

produce considerable variations in fatigue strength and, consequently, in carrying capacity. Stüssi endeavours to establish the regularity of features by analytical computation. A relationship between the fatigue strength of an unnotched and of a notched bar is assumed to be linear with f_m within the entire range of stresses. Thus:

$$K_f = \frac{f_{Kerb}}{f}$$

where:

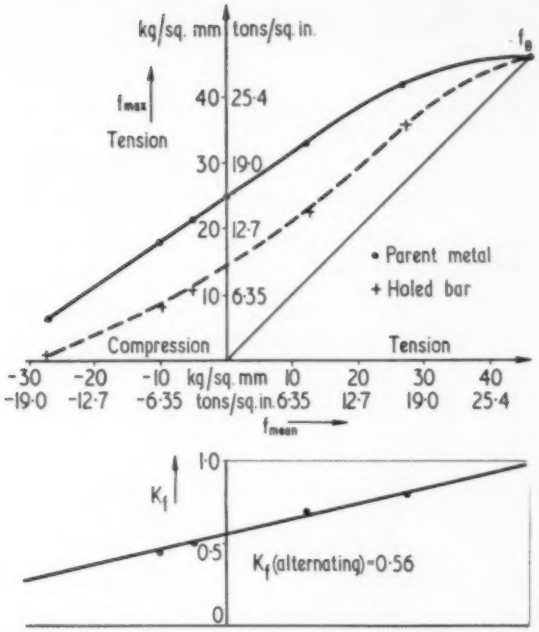
f = fatigue strength (max stress) of unnotched bar
f_{Kerb} = fatigue strength (max. stress) of notched bar.

It is the essential presupposition for this derivation that the tensile strength (f_B) of the notched specimen is approximately of the same order as that of the unnotched bar. This condition is fulfilled in long-time tests in which, with ductile materials, a compensation of stresses can be obtained in the plastic range. However, for normal tensile tests of deeply notched bars this assumption is far from correct. On the contrary, the results of Ludevik's investigations proved there to be a considerable increase of tensile strength on bars with notches producing a multi-directional stress pattern. Figure 2 demonstrates one of a great number of examples with a plain and a holed specimen of aluminium alloy (24 S–T).

For the factor K_f the following results are obtained:

For $\frac{f_{min}}{f_{max}} = +1$ and $\frac{f_{mean}}{f_{max}} + 1$ (*i.e.* static stress)

$$K_f = 1 \; (f_m = f_B)$$



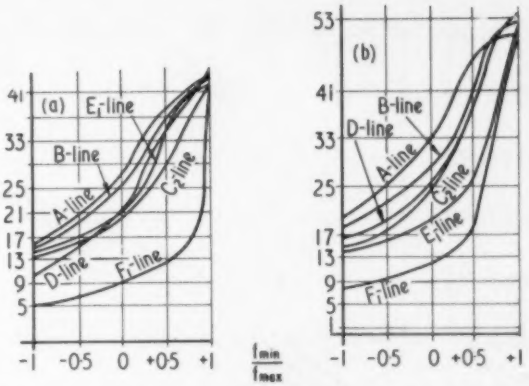
2—*Calculation of factor* k_t *for Wellgren tests on aluminium alloys (24S–T). Axial loading (Stüssi)*

For $\frac{f_{min}}{f_{max}} = -1$ and $\frac{f_{mean}}{f_{max}} = 0$ (*i.e.* alternating stress exclusively) K_f (alternating) has to be determined as a characteristic value.

The evaluation of some test series indicates that Stüssi's assumption is subject to some scattering. On the other hand it has been shown that there must exist a mathematical law relating the fatigue strengths of unnotched and of notched bars.

Curves of Minimum Fatigue Strength

The present work, with the aim of extending the fatigue strength theory of Stüssi, has been based on the foregoing assumption that there exists a linear relationship between fatigue strength diagrams of notched and of unnotched specimens. Though this linear relationship could not be proved in all cases, distinct relationships between notched specimens on the one hand and between notched and unnotched ones on the other hand are supposed to exist. It is obvious that the universality of such a relationship cannot be proved merely by a single test series.



3—*Fatigue strength lines of Kollmar–Kloppel's tests:* (a) *St 37;* (b) *St 52*

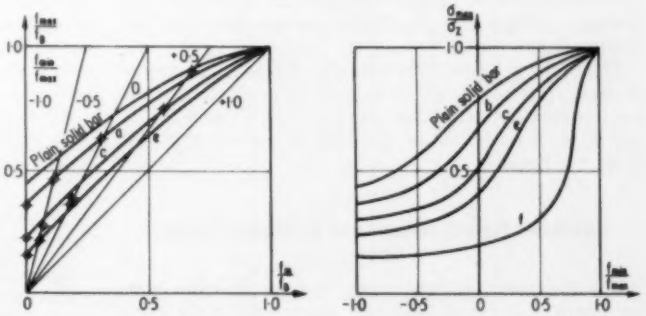
During the years 1956–1957 numerous tests were carried out on a large scale by the Deutsche Bundesbahn (German Federal Railways).[3, 4] Although the reported data were of great importance, a uniform survey has not been achieved (Fig. 3). Stüssi evaluated these tests in accordance with the fatigue strength theory, with some simplifications (Fig. 4).[2] The notch effect must be based on indisputable laws so that differences in fatigue strength are rather improbable.

To establish uniform relationships of the notch effect on all welded joints with regard to their fatigue strengths, the following method has been used for the present experimental work:

It is anticipated that the fatigue strength values of all structural members made of any constructional material (St 37 and St 52) are proportional to the fatigue strength values of the material itself.

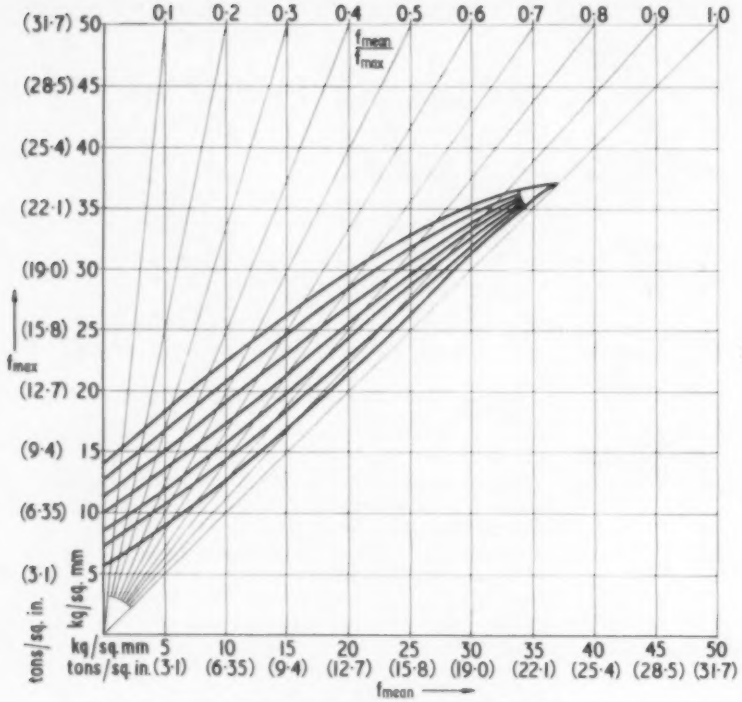
On this basis, and evaluating Stüssi's assumptions, sets of curves for structural members notched to different depths were analytically computed. Thereby, the notch value $K_f = f_{Kerb}/f$ was chosen to take values between 0·3 or 0·4 and 1·0, under alternating load (with intervals of 0·1), and a linear relationship was assumed to exist between K_f and f_{mean}, with $K_f = 1·0$ at $f_{mean} = f_B$. With the respective notch factors, the fatigue strength curves have been computed and the results plotted on a fatigue strength diagram according to Smith. The calculation was made separately for St 37 and for St 52.

Figure 5 shows, as an example, the calculation of the set of curves for St 37.

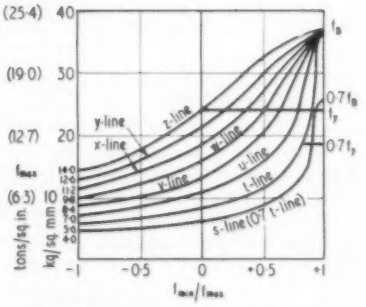


(*a*) Solid bar with as-rolled surface ($k_f \cong 0·92$)
(*b*) High-grade butt weld
(*c*) Normal grade butt weld ($k_f \cong 0·74$)
(*e*) Cruciform joint with double-bevel welds ($k_f \cong 0·66$)
(*f*) Cruciform joint with fillet welds

4—*Evaluation of tests on St 37 carried out by Deutsche Bundesbahn in 1957 (Stüssi)*



5—*Calculation of set of curves for St 37*



6—*z–s lines of welded joints on St 37*

A general rule has long been practised, which enables minimum strength values of various materials to be predicted for static tensile tests; *e.g.*, in the case of St 37 up to 37 kg/sq.mm (23·3 tons/sq.in.), although with individual tests higher values were frequently obtained. These minimum values of static strength form the basis for the equations and for determining allowable stresses under static loading. Similarly, nominal values of fatigue strength (minimum fatigue strength values) are graphically represented, and are also used as a basis.

From these bases, theoretically determined fatigue strength lines for notch factors of different magnitude have been established for the steels St 37 and St 52, and Figs. 6 and 7 show the sets of curves obtained (with *z–s* lines).

Examples from Evaluation of Published Fatigue Tests

The theoretically determined fatigue strength lines for notch factors of different magnitude have been compared with test results. For this purpose 30 original fatigue test series, carried out by the ZIS, and 60 test series which had been reported in the literature during the last 10 years have been evaluated. For more than 6 years, fatigue tests have been carried out on a large scale on welded joints (with particular reference to large structural members as a whole), in the Central Institute of Welding of the DDR (ZIS). Test specimens and many engineering components have been and are being tested in 6–8 fatigue testing plants, by means of pulsators, vibrators etc. Every year 1000–2000 specimens and structural members are being tested dynamically.

Test comparisons and conclusions are classified into special groups which are supposed to show similar values of strength and fatigue strength. These groups are:

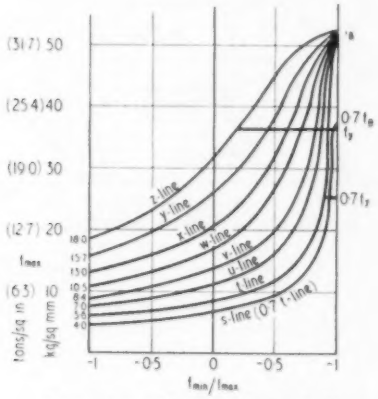
(i) Parent metal—unaffected (plain material)
(ii) Parent metal—affected by welding (transverse seams, longitudinal seam ends)
(iii) Butt welds
(iv) Double-bevel butt welds
(v) Fillet welds

(vi) Web-to-flange welds of transversely loaded beams
(vii) Rigid connections
(viii) Frame corners
(ix) Panel points.

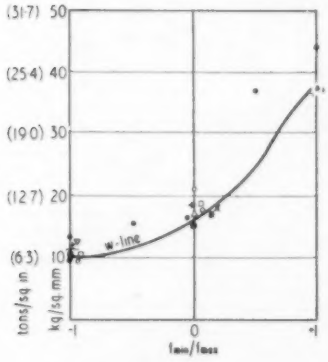
These groups are subdivided into smaller ones considering service duty, loading, materials, welding procedures, etc. Preferably, *equal performance conditions* (*i.e.*, number of load cycles of the different types of welded joints) have been taken into consideration.

From the results of evaluation and comparison of all these tests, fatigue strength values can be deduced which approximate to the effective stength, *i.e.*, the fatigue strength values of welded structural parts manufactured in industrial plants, and these can be considered as minimum values.

Comparing the test results with the theoretically determined fatigue strength lines *z–s* (Figs. 6 and 7),

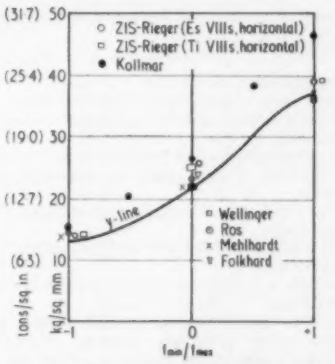


7—*z–s lines of welded joints on St 52*

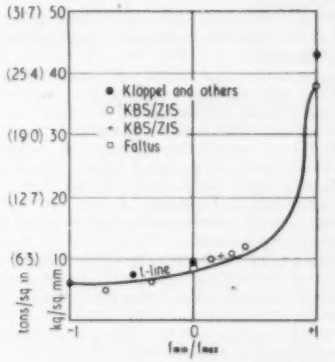


O Dutschinskii
+ Mehlhardt/Graf (related to cross-section of test piece with pulsating tension fatigue strength = 10 kg/sq.mm)
□ Stallmeyer
◉ Folkhardt
— s line related to cross-sectional area of weld $\left(\frac{\text{test piece}}{\text{weld throat}} = 0{\cdot}7\right)$

11—*Fatigue strength diagram for longitudinal load-carrying fillet welds in St 37 (related to cross-sectional area at weld throat)*



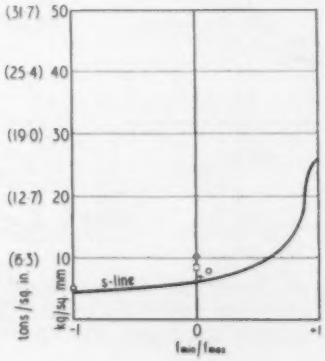
8—*Fatigue strength diagram for high-grade butt welds in St 37, machined flush (100% radiographed) Axial loading*



10—*Fatigue strength diagram for transverse load-carrying fillet welds in cruciform joints of St 37 (related to cross-section at toe of weld)*

○	ZIS	—Rieger —Es VIIIs, horiz.	(axial loading)
□	ZIS	—Rieger —Ti VIIIs, horiz.	(axial loading)
●	Kollmar	—Manual	(axial loading)
△	Stallmeyer	—Manual	(axial loading)
•	ZIS	—Submerged-arc	(axial loading)
◎	Dutschinskii	—Submerged-arc	(axial loading)
×	ZIS	—Square butt	(axial loading)
▽	ZIS	—Manual	(bending)
—	ZIS	—CO_2 welding	(bending)
+	Ros	—Manual	(axial loading)
■	Thum-Erker	—Manual	(bending)

9—*Fatigue strength diagram for normal grade butt welds in St 37, unmachined (100% radiographed, horizontally welded). Axial loading or bending*



various types of joints will be identified with the reference-lines as follows:

z-line = Parent metal—unwelded.

y-line = Butt joint—high grade—flush machined.

x-line = Axially loaded-member with gusset-plate burnt out of the solid material, rounded off.
Transversely loaded beams with web-to-flange joints consisting of continuous double-bevel butt or fillet welds

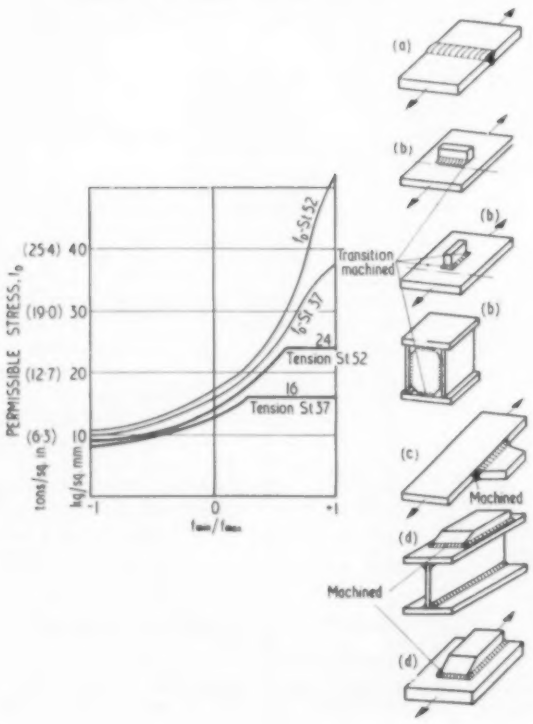
w-line = Butt joint—normal grade, unmachined. (100% radiographed)
Axially loaded-member with transverse fillet weld—machined
Axially loaded-member with longitudinal weld end—machined
Axially loaded-member (including beam flange) with gusset-plate welded on—rounded off and machined
Parent metal at the end of a welded on cover plate—plate bevelled, transition machined.

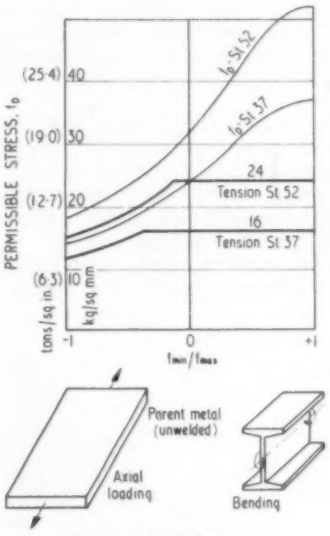
v-line = Double bevel weld—unmachined, without defects
Axially loaded member with transverse fillet weld—unmachined
Beam with transverse weld—unmachined
Axially loaded-member with longitudinal weld-end—unmachined
Beam with longitudinal weld-end—unmachined
Beam with stiffeners welded on—unmachined
Parent metal at the end of a welded on cover plate (transverse fillet weld)—transition unmachined.

u-line = Butt joint—unmachined, vertically welded, overhead welded, not radiographed (small defects possible)
Double bevel weld—unmachined, not radiographed (small defects possible)
Beams with web-to-flange joints consisting of intermittent fillet welds.

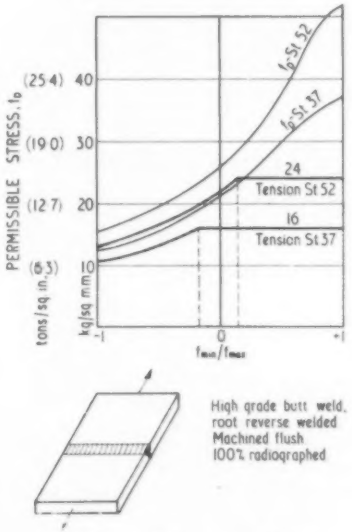
t-line = Transverse load-carrying fillet (cruciform joint)—related to the cross section of the structure.
Longitudinal load-carrying fillet (strap joint)—related to the cross section of the structure
Axially loaded-member with gusset plate welded on, rectangular—unmachined.

s-line = Transverse load-carrying fillet (cruciform joint)—related to the cross-sectional area of the weld throat
Longitudinal load-carrying fillet (strap joint)—related to the cross-sectional area of the weld throat.





12—*Permissible stresses, z line*



13—*Permissible stresses, y line*

(*a*) Normal grade butt weld, root reverse welded. Unmachined. 100% radiographed. Only horizontally welded

(*b*) Axially loaded member and beam with transverse fillets and longitudinal weld ends. Machined, *e.g.*, beam with stiffeners welded on; member with diaphragms welded in

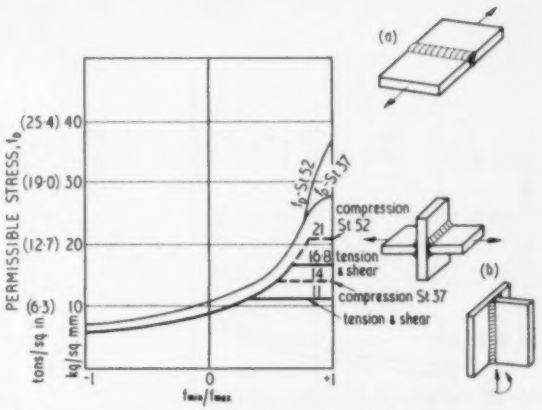
(*c*) Axially loaded member and flange of beam. Machined, with rounded-off gusset plate

(*d*) Axially loaded member and beam at end of welded-on cover plate. Bevelled, and transition machined

14—*Permissible stresses, w line*

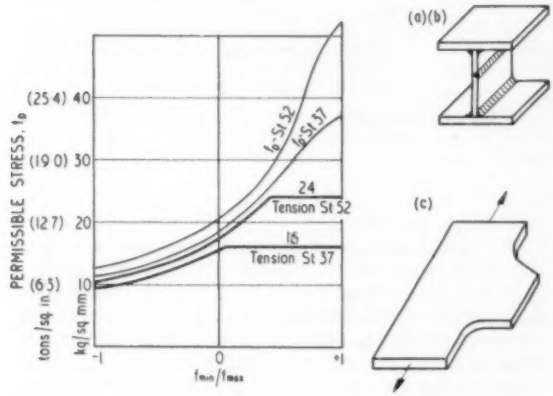
Examples drawn from the 39 fatigue strength diagrams established up to now are shown in Figs. 8–11. They deal with fatigue strength values of welded joints, such as butt and fillet welds on beams etc. Nearly all types of welded joint (transverse welds—longitudinal weld ends, etc), as well as all-welded structural parts have been tested and evaluated. The influence of the welding operation on the parent materials has also been studied.

Since it is not possible within the scope of this brief paper to describe each test in detail (*e.g.*, definitions of parent metals, trial runs, and types of specimen) or to reproduce exactly the relevant passages from the literature used, all this is being reserved for a more comprehensive work[5] to be published later. In fact, this work does not claim to be complete. Many test



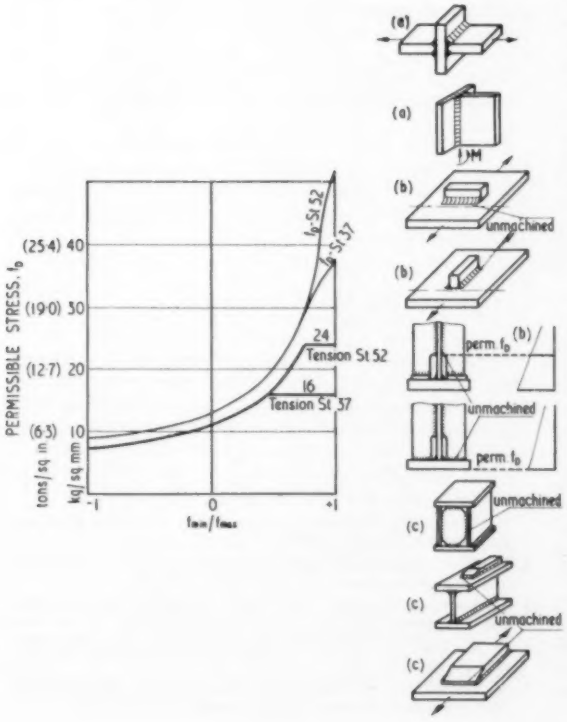
(*a*) Unmachined butt weld, root reverse welded. Random tested by X-rays. Horizontally, vertically, overhead welded
(*b*) Unmachined full penetration double-bevel butt weld. Not radiographed. Weld transverse to stress direction. Rigid beam connection (max. stress)

17*a*—*Permissible stresses* u_1 *line*



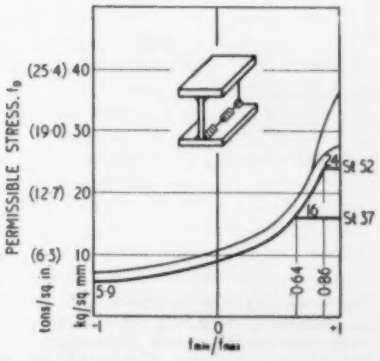
(*a*) Transversely loaded beam, with web–flange joints of continuous double-bevel butt welds or fillet welds
(*b*) Transversely loaded beam, with longitudinal butt joints. Principal stress related to cross-section of beam for web–flange welds and longitudinal butt joints
(*c*) Axially loaded member with 'gusset plate' burned to shape out of solid material. Transition rounded off

15—*Permissible stresses*, x *line*



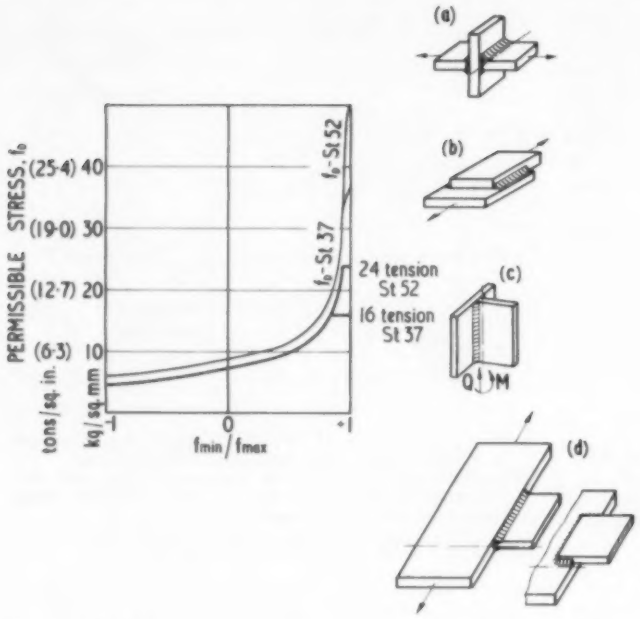
(*a*) Unmachined full penetration double-bevel butt weld. 100% radiographed. Weld transverse to stress direction. Rigid beam connection (max. stress)
(*b*) Axially loaded member with transverse welds and longitudinal weld ends. Unmachined (*e.g.*, beams with welded-on stiffeners; member with welded-in diaphragms)
(*c*) Axially loaded member and beam at end of welded-on cover plate. Transition unmachined

16—*Permissible stresses*, v *line*



Transversely loaded beam, with web–flange welds of intermittent fillets. Related to cross section of beam

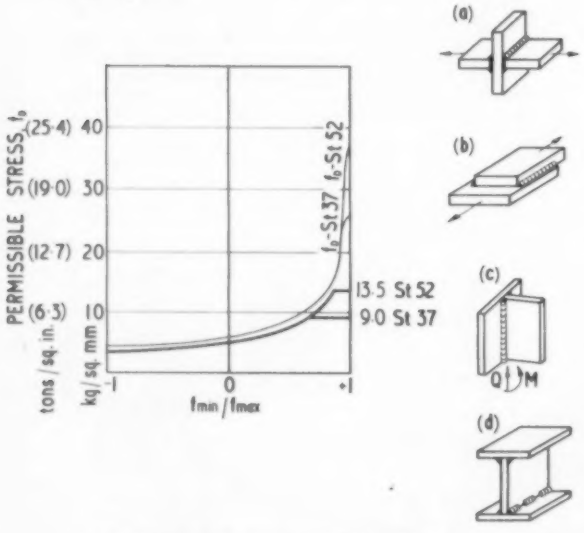
17*b*—*Permissible stresses*, u_2 *line*



(*a*) Axially loaded member with transverse load-carrying end fillets (cruciform or strap joint)
(*b*) Axially loaded member with longitudinal fillets (strap joints) and ends, loaded in shear
(*c*) Rigid beam connection with fillet welds
(*d*) Axially loaded member and flange of transversely loaded beam with gusset plate (junction plate) welded on by fillet or butt welds. Rectangular transition, unmachined

18—*Permissible stresses,* t *line (related to cross-section of specimen)*

series are still missing, particularly those concerning fatigue strengths under alternating stresses (tension-compression) and under pulsating stresses (pulsating tension) under very high pre-stress. However, it is the intention to evaluate accurately all fatigue tests carried out on an international scale and to draw conclusions from such results with regard to further developing the theoretical determination of fatigue strengths of welded joints.



(*a*) Transverse load-carrying fillet welds
(*b*) Longitudinal load-carrying fillet welds
(*c*) Rigid beam connection with fillet welds
(*d*) Intermittent web–flange fillet welds on transversely loaded beams. Welds loaded in shear

19—*Permissible stresses,* s *line (related to cross-sectional area of weld throat)*

The advantages of minimum-fatigue-strength lines are that they are subject to a uniform law and that they have a steady direction. Further studies will show whether the established law ought still to be corrected to some extent or whether it has been laid down definitely. In any case, it can already be stated that minimum-fatigue-strength lines will serve as a basis for a systematic calculation of allowable stresses under static and dynamic loads.

Proposal for Permissible Stresses of Dynamically Loaded Welded Joints in Steel Constructions

From the proposed systematization of fatigue strength values, permissible stresses for welded structures can be derived. As an example, the following basis is proposed for computing welded steel structures (railway bridges, cranes, conveyers). The factors of safety are:

(i) For static loading $y = 1{\cdot}5$ to yield point
(ii) For dynamic loading $y = 1{\cdot}2$ to endurance limit.

In compliance with the minimum-fatigue-strength lines *z–s*, Figs. 12–19 demonstrate proposals for permissible stress values of St 37 and St 52. The types of welded joint concerned are represented figuratively.

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SYMPOSIUM ON FATIGUE OF WELDED STRUCTURES

Fatigue Strength of Butt Welds in Mild Steel

By R. P. Newman,
A.M.I.MECH.E.

The paper contains a brief review of methods used for the determination of the fatigue properties of welded joints, and discusses various factors that influence these properties in mild steel butt welds. Several types of butt weld are considered, mainly transverse, transverse welded from one side, longitudinal, and discontinuous longitudinal. Typical data are quoted to illustrate that there can be wide variations in fatigue strength, depending on joint form; some of these data are compared with design rules.

THE justification for a paper dealing solely with the fatigue behaviour of fusion welded butt joints, confined moreover to joints in mild steel, may at first sight appear to be questionable. In design practice it is, after all, axiomatic that a butt weld should be used if good fatigue performance is required. And again from the viewpoint of design, permissible stresses have been clearly stated, with little divergence of view between various national specifications. The knowledge that the wider field of welded construction will be covered by the collected papers of the Symposium is partly a justification, but more properly it is hoped that a discussion of the factors that influence the fatigue properties of butt welds will show that some care in the use of this type of joint is adequately repaid.

Fatigue failures of mild steel butt welds in actual structures have occurred, although the incidence appears to be far less than with other types of joint. The reduced failure rate is, of course, to be expected; the relative populations of weld type and the higher intrinsic fatigue strength of butt welds weigh in favour of this kind of joint if a simple comparison of the numbers of casualties is made. Unfortunately it is not possible to pursue the interesting topic of service experience in any detail, since many of the case histories remain in confidential files, to the detriment of general design knowledge. Nevertheless, service experience has often been paralleled by observation in the laboratory, and it is possible to underline certain design and manufacturing aspects knowing that they are not without practical relevance. The theme of laboratory observation is, therefore, in the foreground of the present discussion, and at the outset it may be useful to review briefly some of the methods used in laboratory work.

Paper to be presented at a Symposium on Fatigue of Welded Structures, to be held at Cambridge University on 29th March–1st April, 1960.

Mr. Newman is Chief Research Engineer of the British Welding Research Association.

Experimental Techniques

By far the greater amount of work done in studying the fatigue behaviour of welded butt joints has involved direct stress (axial) loading of flat plate specimens. The representation of a practical form of joint is universal, and is carried to the extent of testing specimens made in plate of considerable thickness, so that the use of testing machines of high load capacity (up to 100 tons) is not uncommon. Bending tests of plate specimens are much less frequently undertaken, although some work of this kind has been reported.[1] Beam-type specimens have also been used for the study of splice joints in girder sections[2,3] and for the investigation of circumferential butt joints in pipe.[4] Beam tests can, however, be an alternative choice to direct stress testing, as, for example, in the examination of transverse flange joints when sensibly uniform direct stress conditions may be obtained.

All these methods produce uniaxial stressing. The state of biaxial stress is, of course, appropriate to pressurized components, and pulsating pressure testing of model vessels and of pipe line elements is a well established technique. Tests of simpler, flat plate specimens under hydrostatic pressure have also been developed[5] but primarily with the object of determining high-strain fatigue data. It is not intended to bring into this paper a consideration of pressure vessel details, nor will high-strain fatigue problems be specifically dealt with. These are matters presently distinct from the more general structural field, although extensions of the work on the plastic theory of structural design will eventually be concerned with repeated high-strain conditions. Brief reference will, however, be made to some exploratory work on welded pipes subjected to pulsating pressure.

Conventionally, fatigue tests are carried out under conditions of constant stress amplitude, and for a given type of specimen a fatigue determination is made in the form of a simple S/N curve extending to endurance limits of 2 to 10 million cycles (for mild steel). So called 'programme testing', wherein a predetermined load or stress spectrum is applied to represent statistical load frequency distributions occurring in service, is a relatively new laboratory technique, evolved as a result of attempts to translate the S/N curve into a significant criterion of service performance. As yet, programme testing has not been widely used for the life assessment of welded structures, and what is probably the first development in this direction, in the United Kingdom at least, is referred to in another of the symposium papers.[19]

To return to conventional testing, the procedure followed in making a single fatigue determination is quite simply to vary the stress range applied in successive tests until sufficient data for a given type of specimen are obtained to represent the stress-range/life relationship that is the S/N curve. Since there is technological interest in establishing the influence of mean stress on the maximum stress range that can be sustained for a given life, the test method may well include a series of fatigue determinations, each related to a particular level of mean stress. From the results of such a series of tests, fatigue strength diagrams that show the influence of mean stress at a specified endurance level can be built up. Several forms are used; for example, maximum and minimum stress in the cycle plotted against mean stress (Smith diagram); stress range plotted against mean stress (Haigh diagram); or maximum stress plotted against minimum stress (Gerber or Goodman diagram, *see* Fig. 12).

Most published data for mild steel butt welds refer to two stress ratios:

(i) Minimum stress (f_{min})/maximum stress (f_{max}) $= -1$
('alternating' stressing, with mean stress zero)
(ii) (f_{min})/(f_{max}) $= 0$
(the lower limit of stress f_{min} is zero, and f_{max} some value of tension; frequently referred to as 'pulsating tension').

These are the conditions common to uniaxial stress tests, and while they are not significantly different (between the limits mean stress $=0$ to $\frac{1}{2} f_{max}$, the stress range is the primary factor) the use of one or the other may be dictated by the type of loading system available. This does not necessarily mean that if bending and direct stress tests are made with the same stress ratio the results will be identical in terms of the observed fatigue strength. Reference has previously been made to the possible interchangeability of such tests for studying joints in girder flanges, but here the flange is considered as a separate element under more or less uniform stress conditions. In other cases the existence and influence of a stress gradient would need to be recognized.

Comparison of Reported Fatigue Strength Values

In making a review of published results from fatigue tests of mild steel butt welds it is soon obvious that one is handling highly endemic data. Commission XIII "Fatigue Testing", of the International Institute of Welding recognized this difficulty when it inaugurated an enquiry into methods of testing, and began the task of achieving sufficient standardization of technique to enable co-operative testing to be undertaken with confidence within its membership. This task is not yet completed and there have been setbacks, as the first report of what were intended to be comparative tests indicates.[6] Nevertheless the effort has already produced eminently worthwhile results. Among the first of the numerous factors to be considered was the accuracy of load indication of push-pull machines. Using a given calibration method, members of the Commission reported back on the errors found in machines at their disposal and immediately revealed a situation that needed restoring. The measures taken included securing the agreement of testing machine manufacturers to guarantee the accuracy of new hydraulic machines to within $\pm 2\%$ of the actual load or $0{\cdot}5\%$ of maximum load, whichever would be the greater. The benefits ultimately to be derived from this agreement need no emphasis here.

But while in the future the task of the reviewer may be made that much easier, the problem of presenting an indubitable account of fatigue properties is today quite real, and it is tempting to take refuge in the homily "*no one believes experimental data except the man who takes it, but everyone believes the results of a theoretical analysis except the man who makes it.*"* Believing his own experimental data the present author will quote them freely, and whilst not attempting to compile a catalogue will refer to other selected results which broaden the scope of the paper. In this way it is proposed to consider the fatigue behaviour and strength of transverse and longitudinal butt welds and briefly to touch on such matters as the influence of defects and the relationship between experiment and design.

Transverse Butt Welds

This kind of weld is most often represented by the simple flat plate specimen shown in Fig. 1*a*. Tested either in bending or in direct stress, fatigue failure will invariably be initiated at the edge of the weld reinforcement (Fig. 1*b*) and more than one discrete fracture may be obtained, especially at high stress levels. Thus if the weld is of symmetrical form the fracture will propagate in the parent plate (Fig. 1*b*) and if it is of asymmetrical form it may propagate partly in weld metal (Fig. 1*c*). If a manual weld fails in the latter mode the appearance of the fracture surface can be distinctly different from that of a fracture in the parent plate; for example, some indication of weld metal defects would be seen more often than not. Two fracture surfaces illustrating this point are shown in Fig. 2. No undue significance attaches to the difference in appearance of fracture; in each case the failure is associated with the stress concentration existing at the edge of the reinforcement.

In this kind of specimen, therefore, as in many other kinds of welded specimen, the basic problem may be defined as one involving the notch sensitivity of the

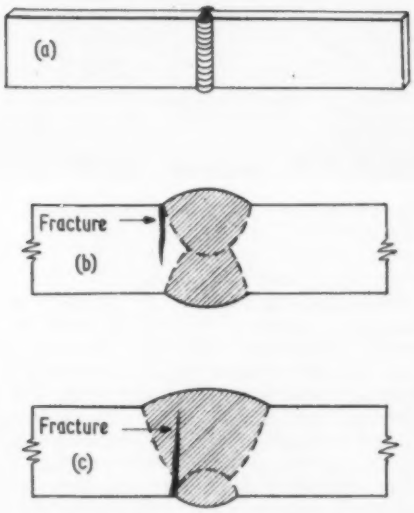
* From *Testing Topics*, published by Baldwin-Lina-Hamilton Corp., Philadelphia, Pa., U.S.A., Jan./Mar. 1953.

heat-affected zone. It is sufficiently well established, however, that for mild steel any attempt to reduce the severity of the notch is far more rewarding than any steps that might be taken to ameliorate the heat-affected zone. More than twenty years ago Kaufmann[7] showed a direct similarity in the fatigue properties of mild steel plates with transverse beads produced either by machining or by depositing weld metal, and since then ample evidence of the fact that the heat-affected zone is not intrinsically weak in fatigue has been collected. Table I contains results selected from three investigations, carried out by Newman and Gurney,[8] Wilson, Munse, and Snyder,[9] and by Kollmar,[10] in which axial load tests of transverse butt welds in mild steel plate were made with the weld reinforcement machined off.

Among the three investigations, various modes of failure were observed, depending in part on the design of the specimen. Failure of the heat-affected zone was infrequent, however, and its properties could not be said to have been thoroughly tested, even under conditions liable to produce fracture of unwelded black plate. On the other hand weld metal failure occurred often, and it is clear that freedom from welding defects is a necessity if 100% of the black plate strength is to be achieved in machined butt welds.

Table I
Fatigue strengths of mild steel plate and machined butt welds

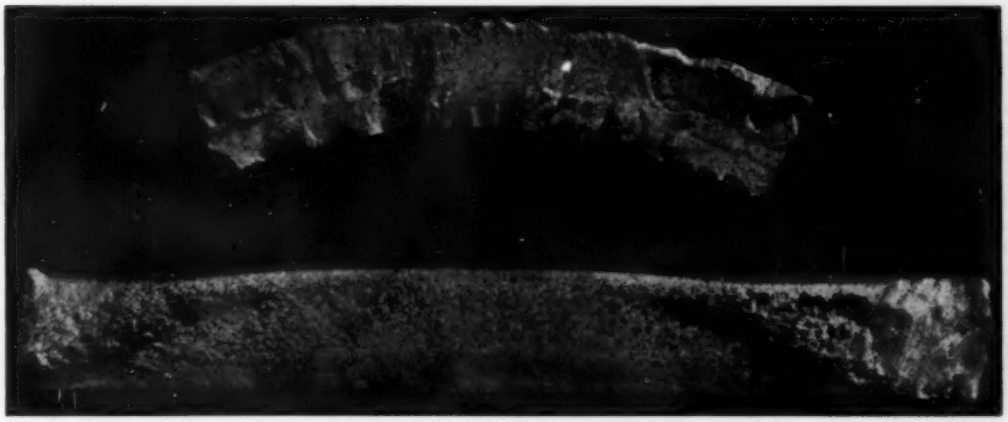
Ref.	*Parent Material*	*Fatigue Strength (tons/sq.in.) at 2×10^6 cycles, $f_{min}/f_{max}=0$*	
		Black Plate	*Machined Butt Weld*
8	½ in. BS.15 C, 0·185% Si, 0·05% Mn, 0·57%	16	16
9	½ in. ASTM–A7 C, 0·21% Mn, 0·48%	15½	12–13½
10	$\frac{9}{16}$ in. ST37 C, 0·14% Si, 0·29% Mn, 0·45%	16¾	16½



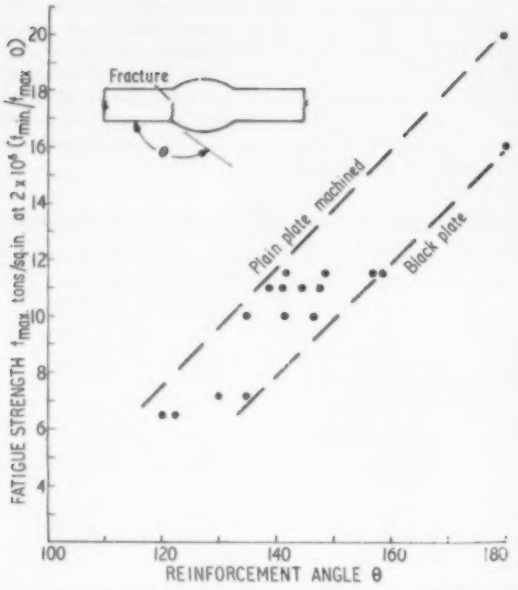
1—*Transverse butt weld specimen and modes of failure:* (a) *Typical specimen form;* (b) *fracture propagated in plate;* (c) *fracture propagated predominantly in weld metal*

The removal of the reinforcement from butt welds is advocated and, indeed, very widely practised when the best conditions for radiographic examination are required. The same recommendation could be made where fatigue strength was judged to be a critical requirement, but since some reservations about the method of removal are necessary (Table I refers to *machined* joints) it is natural to find that there is more interest in acquiring good fatigue strength in 'as-welded' joints. For the transverse butt weld, therefore, the problem reduces essentially to the influence of the stress concentration produced by the change of section at the edge of the weld.

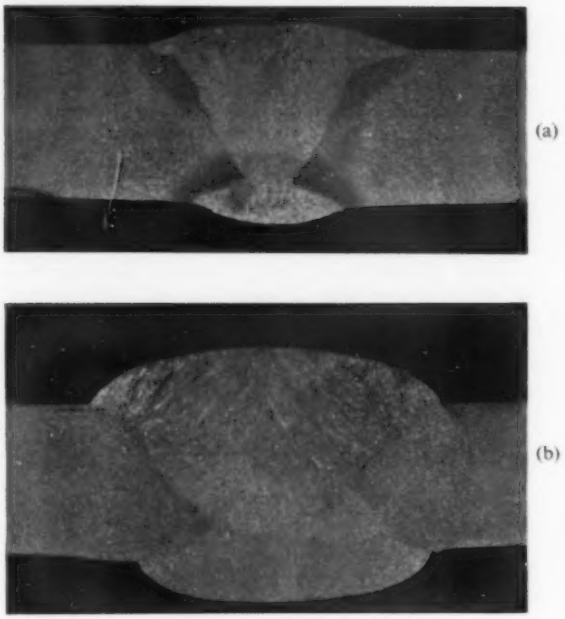
Probably many welding standards would be satisfied that a 'good' butt weld was one made with full penetration and with freedom from defects, including undercutting. Some, however, go further in requiring a smooth and regular contour to the external surfaces



2—*Fatigue fracture surfaces in* (a) *weld metal;* (b) *plate*



3—*Influence of reinforcement shape on fatigue strength of transverse butt welds*



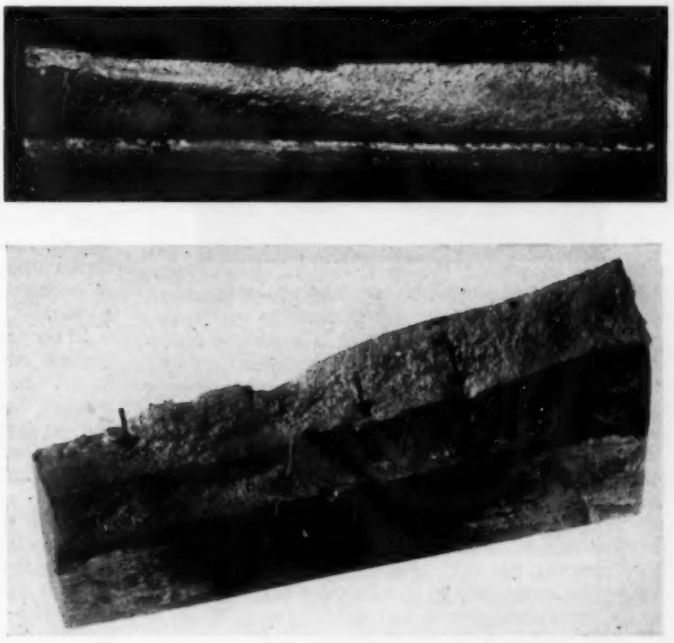
4—*Reinforcement shapes in butt welds*: (a) *High strength joint*; (b) *low strength joint*

of welds, and the absence of excessive reinforcement. These latter qualifications are not without importance in respect of fatigue behaviour, although correlation is obviously difficult. Moreover, the exact shape of weld reinforcement is seldom specified in reports of experimental work in such detail that any correlation could be attempted, and this is where broad comparisons will include another uncontrolled variable. Wilson and co-authors[11] were the first to consider whether weld profile and fatigue performance could be related by using an arbitrary profile rating depending upon the height of the reinforcement and the angle at its junction with the plate surface; in effect, they achieved a reasonable separation of specimens into groups. This approach has been followed by Newman and Gurney,[8] who were able to study groups of specimens with remarkably different fatigue strength values.

The angle θ between the plate surface and a tangent to the reinforcement bead at the edge of the weld (Fig. 3) was taken as a criterion of shape, and was measured in a series of tested specimens containing manual or automatic welds. There were variations in the value of θ in a given weld length, of course, but nonetheless it appeared that where failure occurred minimum θ and the point of crack initiation were coincident. In an attempt to make the measurements critical, θ was determined for a section taken through the point of initiation, and only for specimens lying on or near the mean curve drawn through the results plotted for the whole group. In turn, θ was then plotted against the 2×10^6 cycles fatigue strength value obtained for the group, as shown in Fig. 3 in which the scatter limits are conveniently anchored to two strength values for plain plate ($\theta = 180°$).

It would be quite unreal to develop the idea of establishing an accurate slope for the θ/strength curve or a limiting value of θ. Possibly the two macro-sections shown in Fig. 4 (one from the high-strength (11½ tons/sq.in. at 2×10^6 cycles) group and one from the low-strength group of welds (6½ tons/sq.in.) are of more direct educational value. The use of the graphical presentation does, however, help to emphasize the importance of reinforcement shape and in this case partly to define good and bad shapes. Moreover, without exceeding the variations of shape that occur in practice, the study has shown that sound butt welds can have actual fatigue strengths *below* the working fatigue stress values permitted in design specifications (ignoring any allowance for impact!) A further facet of the investigation is the possible explanation of the different forms of fracture that can be obtained in automatic and manual welds. In the former, initiation over the whole width of the specimen is sometimes observed. This has frequently been seen in 4-in. wide specimens (Fig. 5*a*), and on one occasion a specimen 18 in. wide contained an initiation front over 70% of its width. With manual welds, however, there is usually a definite nucleus—sometimes more than one (Fig. 5*b*). Measured values have shown, as might be expected, a much greater variation of θ in manual welds, with the inference that crack initiation zones must therefore tend to be localized.

To recapitulate, the fatigue strength values shown in Fig. 3 correspond to 2×10^6 cycles of axial loading under a stress ratio of $f_{min}/f_{max} = 0$, and were obtained from full penetration transverse butt welds, free from significant internal defects. For these conditions the lowest strength values quoted in the literature are only marginally lower than the 6½ tons/sq.in. obtained with specimens of the type shown in Fig. 4*b*. It would



5—*Fatigue fractures in transverse butt welds:* (a) *Wide initiation front in automatic weld;* (b) *local initiation area in manual weld*

not be unreasonable, therefore, to take this value as the lower limit of strength for sound welds made in accordance with good commercial practice. The results obtained by Wilson, Bruckner, McCrackin and Beede[12] indicate that a lower limit of 6½ tons/sq.in. would also embrace manual butt welds made under site conditions in the vertical, horizontal, and overhead positions. The black plate strength of say 15–16 tons/sq.in. for normal structural quality mild steel is a definite upper limit, although it is probably attainable only with machined joints. The results given in Table II are more typical of the upper limit values for 'as-welded' joints, being in the range 70–80% of the strength of the black plate.

Transverse Butt Joints Welded from One Side

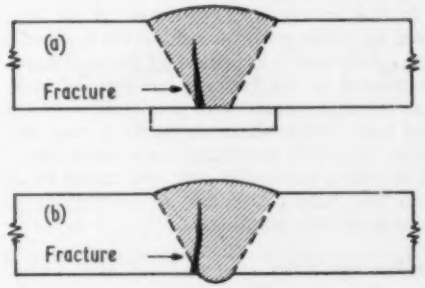
Surprisingly little has been done on the fatigue testing of transverse butt joints welded from one side, although the behaviour of such joints is of interest since failure from the root side of the welds, as shown in Fig. 6 is probable, either for axial loading or reversed bending. For joints made on a backing bar reasonably consistent fatigue properties could, therefore, be expected, whereas for joints with no backing member strength would depend heavily on the skill of the operator in maintaining full penetration.

Table II

Maximum fatigue strength of mild steel butt welds with reinforcement left on

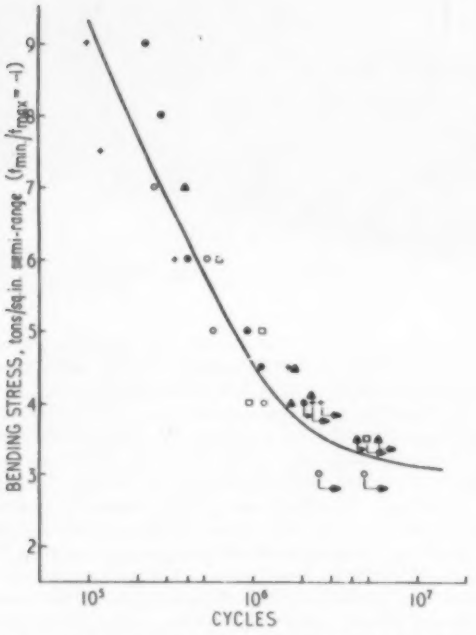
Ref.	*Parent Material*	*Type of Weld*	*Fatigue Strength (tons/sq.in.) at 2×10^6 cycles, $f_{min}/f_{max}=0$*	
			Black Plate	*Butt Weld-Reinforcement on*
8	½ in. BS.15	Manual	16	11½
		Automatic	16	11
11	½ in. ASTM-A7	Manual	15½	11¾
10	$\frac{9}{16}$ in. ST.37	Manual	16¾	13¼

A circumferential butt joint in a pipe subjected to bending or axial load is representative provided that, for the bending condition, the t/D ratio is small. Alternating plane bending tests of 6⅝ in. dia. mild steel pipe with a wall thickness of ⅜ in. have been carried out by the author[4] in an investigation of the influence of welding defects on the fatigue strength of pipe butt joints. To obtain consistency of notch effect at the root, the various series of specimens, which were obtained from different sources, were made with backing rings, and failure from the junction of weld metal and backing ring, as shown in Fig. 7, often occurred even when defects were present. With a nominal t/D ratio of 0·05, sensibly uniform stressing of the pipe wall could be assumed and the failures were, of course, associated with the maximum stress areas lying on the diameter normal to the neutral axis. The results from these tests are assembled in one S/N



6—*Fatigue fractures from root in butt welds made from one side:* (a) *With backing bar;* (b) *without backing bar*

7—*Fatigue fracture initiated from backing ring side of joint*



8—*Results of fatigue tests of pipe butt welds with backing rings*

diagram, Fig. 8. The five different types of plotted point indicate the number of sources of specimen supply—the relative lack of scatter will be noted.

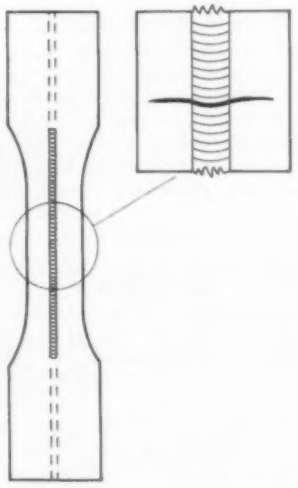
For 2×10^6 cycles a mean stress range of approximately $7\frac{1}{2}$ tons/sq.in. ($\pm3\frac{3}{4}$) was obtained, or about 35% of the plain pipe strength. This result is equivalent to the lowest strength value of $6\frac{1}{2}$ tons/sq.in. referred to in the previous section for a stress ratio of $f_{min}/f_{max}=0$, and is indicative of the significant notch effect present in the backing-bar type of joint. Very similar strength values were produced with metal-arc welded pipe joints when no backing ring was used, provided that full penetration was achieved; in one series in which particular care was taken to obtain a smooth root bead a strength of ±5 tons/sq.in. (50% of plain pipe) was established.

Longitudinal Butt Welds

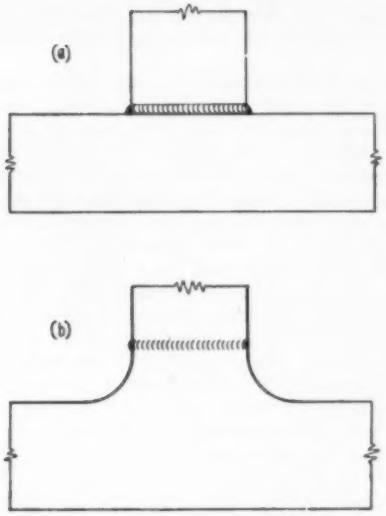
Again there are relatively few experimental results for longitudinal butt welds in mild steel. With a specimen of the type shown in Fig. 9, in which the weld ends are removed from the test zone, the mode of failure indicated is typical. Cracking is initiated in surface ripples on the weld reinforcement and, with manual welds, also at re-strike positions. Tests by Harris, Nordmark, and Newmark[13] indicated 2×10^6 cycle strength values ($f_{min}/f_{max}=0$) of approximately ±11 and $\pm11\frac{3}{4}$ tons/sq.in. for welds made with rutile and basic electrodes respectively in $\frac{3}{4}$ in. thick A.7 steel. Insofar as these figures can be compared with others reported in the literature they may be taken as representative, and it would appear that the dispersion in strength values is notably less than with transverse joints.

In the foregoing tests, failures were initiated both in surface weld ripples and at re-strike positions. It might, therefore, be supposed that the use of automatic welding would result in a useful increase of strength, but no tests to confirm the point have been undertaken. The effect of reducing transverse discontinuities on the surface of the weld has, however, been shown in some exploratory work at the British Welding Research Association. Pulsating pressure fatigue tests were made on $6\frac{5}{8}$ in. dia., $\frac{3}{8}$ in. wall thickness mild steel pipes containing circumferential butt welds, without backing rings. Fatigue fractures were initiated in the bore, in a direction normal to the hoop stress, and for metal-arc welded joints the initiation point was always associated with transverse rippling of the root bead. In contrast, oxy-acetylene welded specimens, having a smoother root bead, failed from score marks in the bore away from the area of the joint. For a hoop stress range of 0–10 tons/sq.in. the scatter limits in life were $0{\cdot}21$–$0{\cdot}58\times10^6$ for the metal-arc joints and $0{\cdot}27$–$2{\cdot}00\times10^6$ for the oxy-acetylene joints.

The condition of the weld surface is thus of some importance in longitudinal butt joints, but good strength values can clearly be achieved with the weld reinforcement left on. If, however, such joints are discontinuous a large loss of strength will result from the effect of the stress concentrations associated with weld ends, as in intermittent fillet welding. Intermittent butt



9—*Fatigue failure of longitudinal butt weld*



10—*Connections of flanges at right angles:* (a) *Butt weld on edge of main member;* (b) *butt weld removed from edge, and with larger corner radius*

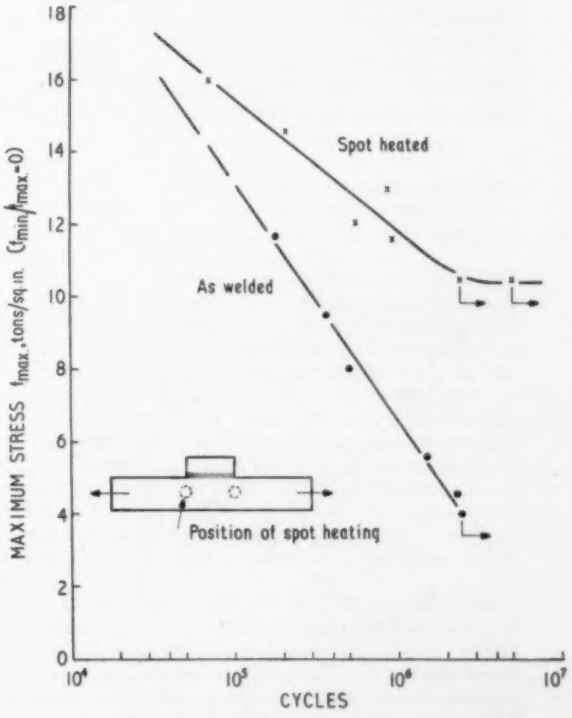
welding is not often used, but is in fact frequently represented in form without being recognized as such. A common example is the attachment of flanges at right angles, as in Fig. 10. Considering the behaviour of the main member under cyclic loading, the major change of section at the attachment must give rise to a significant stress concentration, which would thus effect the fatigue strength irrespective of the presence of the weld. Thus, whilst the detail shown in Fig. 10*a* is convenient in practice, the alternative, shown in Fig. 10*b*, is far superior in respect of the strength of the main member. Such connections are often simulated in laboratory tests by flat plate specimens with short gussets attached to the edges.

Table III contains selected results from three investigations of the fatigue behaviour of the flat-plate type of specimen, carried out respectively by Götzlinger,[14] Puchner,[15] and Gurney and Trepka.[16] The considerable loss of strength associated with the simplest type of connection is evident, as is the superiority of the preferred form in which the weld would be removed from the edge of the main plate. The quite remarkable performance of the simple type when spot-heated will also be noted. This is a relatively cheap process by comparison with the other post-weld finishing treatments depicted in Table III, which are less successful in restoring strength. Indeed, in terms of cost and performance, the method appears to hold an advantage over the ideal design of Fig. 10*b*. It is to be emphasized, however, that spot-heating is the subject of present research, as discussed in another of the Symposium papers (*see* p. 178) and that control of the process and the persistence of its effect will require study so as to establish fully the necessary confidence in its use. Moreover, as the effect is derived from the induction of a residual stress system, so disposed as to subject the notched zone to compression, it is dependent on the applied tension stresses not reaching yield

Table III

Fatigue strengths of plates with butt welded gussets

Specimen	*Fatigue Strength of Main Member, tons/sq.in. at 2×10^6 $f_{min}/f_{max}=0$*		
	Götzlinger[14]	*Puchner*[15]	*Gurney and Trepka*[16]
Black Plate	—	13¼	16
r = 50mm	15 for 45° change of section	10¾	—
r = 50mm	—	7	—
r = 10mm	8¼	7¼	—
Heat spot	5	5 as welded 12½ spot heated	5 as welded 11 spot heated
	—	—	4 as welded 11 spot heated



11—*Results of fatigue tests on plates with butt welded gussets*[16]

value. The increase of fatigue strength must, in fact, be related to a given endurance level, and the S/N diagram in Fig. 11, taken from Gurney and Trepka's results,[16] illustrates that the data in Table III are optimum values in terms of the percentage increase of strength. It also illustrates the need to consider the possible effect of occasional overloads.

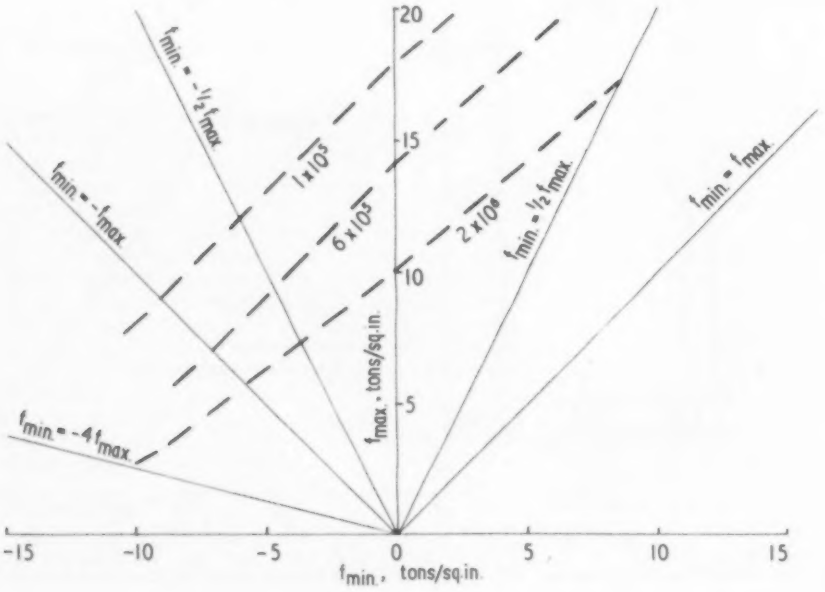
Influence of Weld Defects

The literature on the influence of weld defects on the fatigue strength of mild steel butt welds has recently been reviewed[17] and it is therefore not intended to give any detailed account of present knowledge here. However, no impression should be given that the problem is capable of being tidily contained in this way. The review has served essentially to confirm the complexity of the problem and to define certain areas for further work.

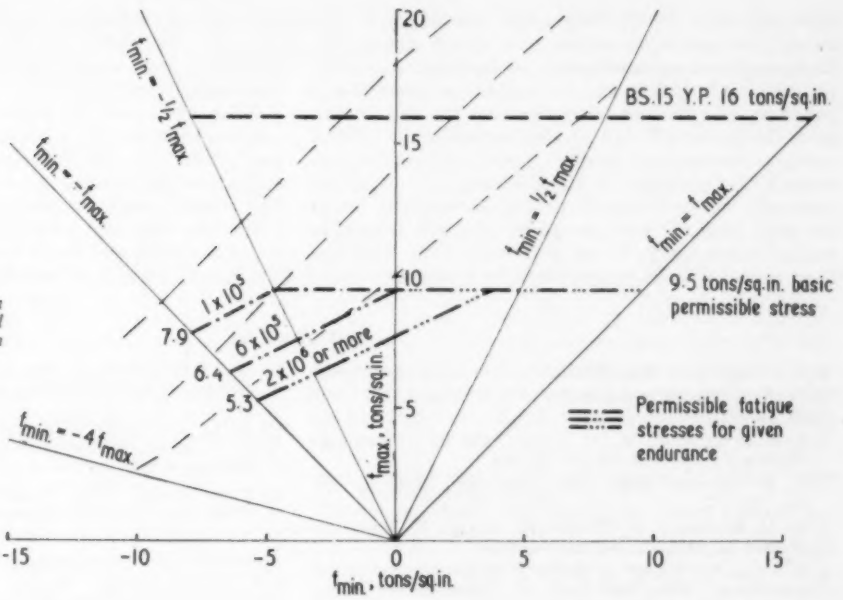
Since fatigue testing is a critical method of testing for quality there is a substantial interest in using it as one means of assessing acceptable standards of welding for many types of structure, apart from those subjected to fatigue loading in service. The interpretation of the results of fatigue test may therefore depend to some extent on the type of application involved. The objective for any new work strictly related to fatigue performance may, however, be stated fairly simply; to show the limits of defect severity at which fatigue strength is affected. It is assumed here that, for certain types of defect at least, there will be positive limits, but sufficient evidence exists to show the assumption to be justified and not merely a misplaced hope. For example, of the five series of results plotted in Fig. 8 only one is for pipe butt welds of good commercial quality; three series of these welds contained a given type of defect (either porosity, slag inclusions or lack of fusion, each occurring quite extensively) and the remaining series was of extremely bad quality as judged radiographically.

It must be appreciated that the fatigue strength of the pipe butt joint concerned—in effect a transverse joint made from one side on a backing member—is comparatively low. A value of 35% of the strength of the unwelded pipe, at 2×10^6 cycles, has been indicated. These particular defects were not therefore the weakest link in the joint. But had they occurred in machined joints in flat plate, with intrinsically 100% strength, there is little doubt that they would have initiated failure and caused loss of strength. Thus one aspect of the problem is the determination of severity limits for different joint forms. When it is realized that such limits will even then depend on the stress system (*e.g.*, axial *v.* bending) and may ultimately have to be stated for combined defects, it will be seen that the statement of the objective is made more easily than its realization.

Most of the experimental work carried out or projected is related to butt welds in flat plate subjected to axial stressing, this being the more general area of practical interest. If now this area is further reduced by considering butt joints welded from both sides and normally associated with strength values in the range 8–12 tons/sq.in. (50–75% strength joints) certain conclusions may be drawn from present knowledge of the subject. For instance, defects such as incomplete penetration, longitudinal cracks, continuous slag lines, or continuous undercutting would be detrimental in transverse joints. These would all occur as discontinuities lying normal to the direction of applied stress. Whilst the effect of some of them, incomplete penetration, for example, could be markedly reduced in a longitudinal joint, the idea of setting up two standards as a perfectly general requirement is not attractive. The omission of porosity and discrete slag inclusions from the list does, however, draw attention to the usefulness of investigating their role rather more



12—*Fatigue strengths of transverse butt welds under axial loading, for given endurances (approx. only)*



13—*Permissible fatigue stresses for butt welds in BS.15 steel under axial loading (based on BS.153:1956)*

precisely, since these are the defects that occur most commonly in practice.

Experiment and design

Most of the fatigue strength data quoted so far have been related to the stress ratio $f_{min}/f_{max}=0$ and the endurance limit of 2×10^6 cycles. The strengths of butt welds tested under other stress ratios and at different endurances is also of interest. However, the extent to which such additional data can be tabulated is obviously limited. The investigations referred to have in large measure been concerned with comparative effects as observed for one type of loading, and it is clear from the already large range of factors requiring study that to multiply the number of tests by, say, several mean-load conditions, would be prohibitive.

It has been seen that for butt welds of otherwise good quality, fatigue strength values can vary significantly. If, however, an average strength value of 10 tons/sq.in. is assumed for the stress ratio $f_{min}/f_{max}=0$ and the endurance of 2×10^6 cycles, strengths at other stress ratios and endurances can be indicated for the type of joint concerned. These are shown in the Goodman diagram, Fig. 12. The three curves for endurances of 1×10^5, 6×10^5, and 2×10^6 respectively are not based on the results of a single investigation, but represent a summary of the data available from several sources which, with some adjustment, permit an estimate of strength to be made between the stress ratio limits of $f_{min}/f_{max}=-1$ and $+0{\cdot}5$. Within these limits the curves are sensibly parallel to the 45° line for $f_{min}=f_{max}$, showing that the stress *range* sustained at each endurance value is not influenced greatly by the increase of mean stress from zero $\left(\frac{-f_{max}+f_{max}}{2}\right)$ to $0{\cdot}75 f_{max}$ $\left(\frac{\frac{1}{2}f_{max}+f_{max}}{2}\right)$. On the other hand it appears likely that the stress range will increase considerably with increasing compressive mean stress.

It is on the basis of evidence of this kind that some design rules have been prepared, notably in the field of bridge construction. Such rules may be typified by the requirements of BS.153:1958 "Steel Girder Bridges" shown, for butt welds, in diagrammatic form in Fig. 13, with the estimated strength curves superimposed. Allowance is made for the recent revision of minimum yield stress of BS.15 steel, and axial loading conditions are assumed for which the basic permissible stress of 9½ tons/sq.in. (*i.e.*, the static working stress) represents the required factor against yield and thus the cut-off level for fatigue stresses.

The diagram shows that dependence on good quality in the sense discussed in this paper is clearly necessary if a positive factor of safety on experimental strength values is to be maintained. It is worth noting that this is perhaps of greater importance in the application of the BS.153 rules to other types of structure, for which no impact allowance adds to the real factor of safety as it does in bridge construction and in which the required life time would accumulate loading cycles greater than 2×10^6. Furthermore, the definition of a butt weld is involved; the exclusion of the discontinuous type previously referred to needs to be emphasized at least in relation to the stress values given in Fig. 13.

The acceptance of the S/N curve based on conventional testing is thus implied in rules that state working stresses for a given life, and normally the calculated maximum stress in the member becomes the significant reference value. Where constant-load amplitude conditions exist the relationship between experiment and design is therefore simplified. On the other hand it is

relatively easy to challenge the conventional S/N curve as a significant criterion of actual service performance when service conditions are characterized by random loading. It would be outside the scope of this paper to take this consideration very far and, in any case, the advent of load spectra testing in the field of welding construction is quite recent. Some development of the principle of life assessment by means of such tests will undoubtedly be seen in this field, but at the same time the part played by constant amplitude testing is not lightly to be dismissed. Joint form has been shown in conventional tests to have a profound influence on the fatigue behaviour of welded structures, and the application of this simple order-of-merit result has, over many years, served usefully to overcome actual service failure.

It has been well illustrated by de Leiris[18] that the fatigue test in general is an effective method, even in its qualitative role, for revealing points of stress concentration and in helping to improve design detailing. The conventional test of a welded joint can equally fulfill this purpose, particularly when several stress levels are employed, even if the quantitative result is not always capable of simple application.

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Fatigue Strength of Fillet Welded Joints in Steel

By T. R. Gurney, M.A., A.M.I.C.E.

The present state of knowledge of the fatigue strength of fillet welded joints in steel is considered in all its aspects. After comparing the relevant clauses of the current design specifications used in Britain, U.S.A., U.S.S.R., and West Germany with some typical experimental results, the paper goes on to consider the effect of the various factors affecting fatigue performance, with particular reference to experimental work carried out since the war. Finally, current work on possible methods of improving the fatigue strength of fillet welded joints is outlined.

Among load carrying steel structures, those subjected to dynamic loading form a special group, because the stresses that can be used in their design depend to a large extent on the detailed design itself. This is particularly true of any welded structure in which fillet welds are used—and it is difficult to visualize any welded structure being designed without their inclusion, if only for the simplicity and versatility of the fillet welding process or on the grounds of fabrication cost.

Unfortunately, as is well known, fillet welds lead to particularly poor fatigue strengths, and this has been amply confirmed by the numerous research programmes carried out since the war, both in Britain and abroad.

Paper to be presented at a Symposium on Fatigue of Welded Structures to be held at Cambridge University on 29th March to 1st April.

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Table I
Properties of mild steels

Investigator	*Ref.*	*Steel*	*Composition, %*					*U.T.S., tons/sq.in.*	*Yield, tons/sq.in.*
			C	*Si*	*Mn*	*S*	*P*		
Gurney	1 and 6	BS.15	0·185	0·05	0·57	0·030	0·023	28·5	18·0
			0·125	0·05	1·24	0·045	0·015	29·8	19·2
	9	BS.15	0·14	0·035	0·76	0·045	0·030	28·9	18·4
Weck	2	BS.15	0·18	0·04	0·67	0·05	0·02		
Wilson	4	A.7	0·23	0·016	0·51	0·041	0·015	26·2	15·4
	8	A.7	0·22		0·46	0·030	0·017	28·2	17·4
Dutschinskii	12, 13	St.3	0·14–0·22	0·12–0·30	0·35–0·65	0·055 max	0·050 max	26–28	—
Folkhard	7	St.37	0·13	tr	0·37	0·042	0·042	25·8	16·7
Wintergerst	5	St.37	0·14	0·29	0·45	0·025	0·039	27·3	17·8

Some of this work had as its object the provision of information so that a detailed review could be made of the standard design specifications in the country in question; in fact, during the past few years, several countries have issued revised design specifications. It is of interest to compare these with each other and with the experimental results obtained, and such a comparison forms the first part of this paper.

The work carried out has covered many aspects of the fillet weld problem, and the second part of the paper is devoted to a summary of the various factors that might be expected to affect the fatigue strength of fillet welded joints. In particular, the effects of stress concentrations, residual stresses, and the tensile strength of the parent material are considered. Finally, a brief survey is made of some recent research aimed at improving the fatigue strength of welded joints, since this is particularly applicable to fillet welds.

Comparison of Design Specifications and Experimental Results

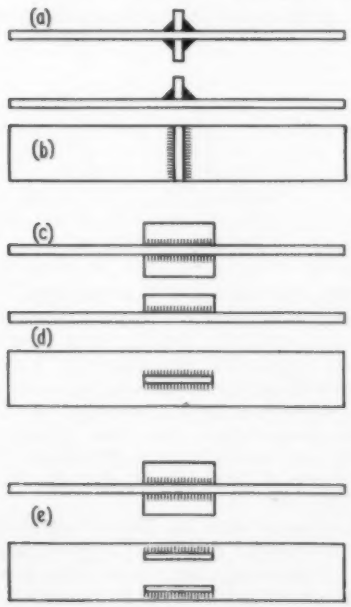
In recent years the following specifications for the design of steel structures subjected to dynamic load have been issued, and the rules for the design of fillet welded joints contained in them are compared with one another below:

(*a*) Great Britain—BS.153:1958 "Steel girder bridges" (with Amendment No. 1 1959)
(*b*) U.S.A.—AWS "Specification for welded highway and railway bridges", 1956.
(*c*) U.S.S.R.—TUPIM–sv–55 "Specification for the design and construction of welded railway bridges", 1955*.
(*d*) West Germany—DV 848—"Regulations for welded railway bridges", 1955.

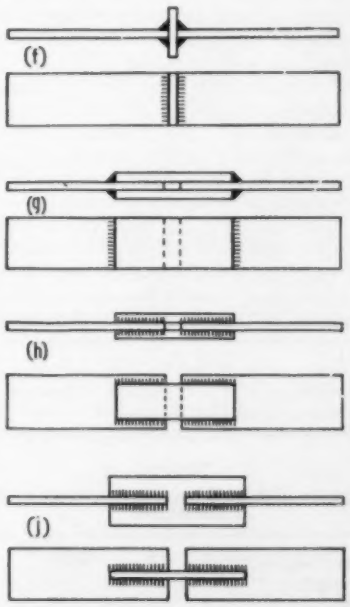
All these specifications refer to the design of railway bridges, but in many instances, and certainly in Britain at the present time, they are the only official rules available for the design of any welded structure subject to fatigue loading.

The comparisons have been drawn between the permissible design stresses for 2×10^6 cycles of loading,

* It has unfortunately proved impossible to obtain the original of this specification. The design stresses quoted have therefore been taken from Dutschinskii[13] and these are believed to be approximately correct.



1—*Details of specimens with non-load-carrying fillet welds*



2—*Details of specimens with load-carrying fillet welds*

using the various types of structural mild steel in common use in the various countries concerned, namely BS. 15 in Britain, ASTM-A.7 in the U.S.A., St. 3 in Russia, and St. 37 in Germany. Typical chemical compositions and mechanical properties of these steels, as used in some of the investigations, are shown in Table I. As far as is known the small differences existing in these materials do not markedly affect fatigue strength, particularly in the severely notched condition.

It is convenient to consider the design of fillet welded joints under three headings:

(*a*) Non-load-carrying fillet welds, for which the expected point of failure is at the toe of the weld for a transverse weld, and at the end of the weld for a longitudinal weld. Details of the various types of specimen used in fatigue investigations of such joints are shown in Fig. 1

(*b*) Load-carrying fillet welds with the same expected points of failure as for non-load-carrying welds. Figure 2 shows details of some typical specimens used for studying this type of connection

(*c*) Load-carrying fillet welds failing in the weld metal.

Non-load-carrying welds

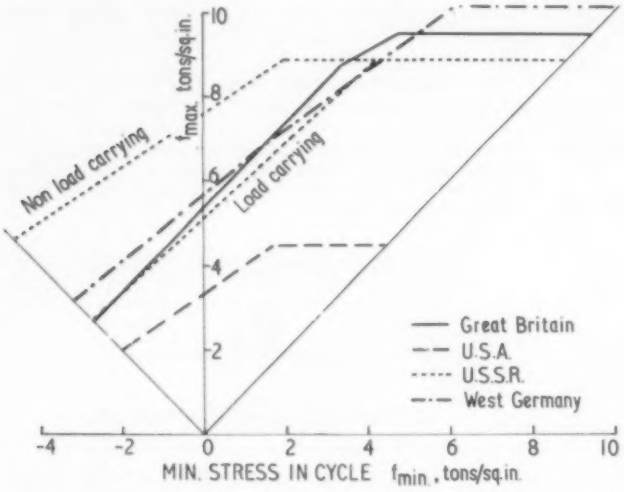
As far as this paper is concerned a non-load-carrying weld might be defined as any weld on a stressed member which does not transmit either the load *in* the member or an appreciable part of the load *to* the member. (An example of the latter use would be the gusset connection at the panel point of a lattice girder). With this definition, it will be appreciated that non-load-carrying welds are very commonly used in practice, and include such welds as those attaching girder stiffeners, machinery foundation pads, brackets, and the like, and the frequency of use has certainly been reflected in the amount of fatigue testing carried out on such joints.

Unfortunately, few of the investigations have covered more than one, or sometimes two, load cycles, but it can be stated that with one or two notable exceptions the fatigue strengths (*i.e.*, the strength of the main stressed member for a given number of load cycles) have proved uniformly disappointing. One of the most complete investigations has been that carried out at BWRA where, using specimens of Type C (Fig. 1), tested in the as-welded condition, the fatigue strengths at 2×10^6 cycles were as shown in Table II.

These figures confirm that the stress *range* required to cause failure of a fillet welded joint remains virtually constant over a wide range of values of f_{min}/f_{max}. The actual fatigue strength values have been more or less confirmed by other investigators. In particular, Weck[2] found a fatigue strength of 5·25 tons/sq.in. on a pulsating tension cycle with specimens of Type E.

With transverse welds the experimental results have been much more variable, the reasons for which will be considered later. Thus, in tests on specimens of Type A (Fig. 1), de Leiris[3] and Dutilleul obtained fatigue strengths between 6·3 and 8·9 tons/sq.in. on a pulsating tension cycle, whereas, with similar specimens, Weck reported a strength of 5·75 tons/sq.in. and Wilson[4] obtained ± 5·8 tons/sq.in. on an alternating cycle. The most complete investigation of a transverse fillet welded joint was that carried out by Wintergerst and Rückerl[5] using specimens of Type B (Fig. 1) fabricated from St. 37, and the results are shown in Table III.

When compared with the fatigue strength of 6·5 tons/sq.in. for similar specimens in BS.15 steel tested at BWRA[6] these results seem to be remarkably high, but on the other hand Folkhard's [7] results scattered between 10·8 and 13·4 tons/sq.in. on a pulsating



3—*Permissible stresses for members with fillet welded joints*

Table II

Fatigue strengths of Type C specimens obtained by Gurney[1]

Cycle f_{min}/f_{max}	−4	−1	0	0·5
Fatigue Strength, tons/sq.in.	1·25—−5	±2·75	0–5·5	5·0–10·0

Table III

Fatigue strengths of Type B specimens obtained by Wintergerst and Rückerl

Cycle f_{min}/f_{max}	−1	−0·5	0
Fatigue Strength, tons/sq.in.	±9	+10·8—−5·4	0–13·7

tension cycle, and Wilson's result of ±8·4 tons/sq.in. tend to confirm the German results.

The permissible design stresses in the various countries for a member subjected to axial loading for 2×10^6 load cycles are shown in the form of a Goodman diagram in Fig. 3. From this it can be seen, by comparing the design stresses with the experimental results already quoted, that the permissible stresses of BS. 153 give no factor of safety against failure at 2×10^6 cycles for non-load-carrying welded joints. It must be pointed out however that the same is not true of shorter endurances, when design stresses are significantly lower than the experimental results. This can be seen from the S/N curves plotted in Fig. 5, where the design stresses quoted in BS. 153 are compared with some typical experimental results for a pulsating tension cycle. For endurances greater than 2×10^6 cycles the position is rather obscure, because very little work has been done. However, Weck[2] showed that for specimens of Type E (Fig. 1) fatigue strength continued to fall after 2×10^6 load cycles, and he recorded a strength of only 3 tons/sq.in. at 10^7 cycles. It would appear that further work is required to determine fatigue strengths at long endurances, since some structures undoubtedly suffer more than 2×10^6 load cycles in their lifetime.

The Russian and West German specifications are much more comprehensive than those used in Britain and the U.S.A. The British Standard 153 gives only two series of design stresses, one of which is applicable to butt and continuous longitudinal fillet welds and the other to all other fillet welded details, but the Russian and German specifications give several different fatigue strength curves for design purposes, each covering a different selection of details. The curves shown in Fig. 3 are the lowest of these, but they would appear to be rather optimistic, in spite of the results obtained by Wintergerst and Rückerl and by Folkhard. In this respect it must be realized that in these two countries even certain fillet welded details can be designed to higher stresses.

Load-carrying welds

In general, the fatigue strengths obtained in tests on load-carrying fillet welds have been even lower than those for non-load-carrying welds. Wilson[8] carried out a reasonably complete investigation using specimens similar to Type H but with additional transverse welds at the ends of the cover plates. Since all his failures occurred in the cover plates at the end of the longitudinal welds the results can, however, be taken to refer to load-carrying longitudinal welds, and the strengths obtained are shown at the foot of Table IV. His strength of 4·3 tons/sq.in. on a pulsating tension cycle has been more or less confirmed at BWRA[9] where strengths varying from 3·0 to 4·5 tons/sq.in. have been obtained, and by Konishi,[10] who recorded strengths of 4·6 and 5·2 tons/sq.in., but Soete[11] has obtained an even lower strength of 2·5 tons/sq.in. All these investigations were made with specimens of Type H. In the BWRA work the lower strength limit of 3·0 tons/sq.in. at 2×10^6 cycles referred to failure in the cover plates, and the upper limit of 4·5 tons/sq.in. to failure in the main plate. The corresponding stresses at 10^5 cycles were 9·25 and 14·25 tons/sq.in. respectively. Where the ratio of the areas of cover plates to main plate was made equal to 1·5 the distribution of failures between the two was random. With specimens of Type J the fatigue strength was found to be 3·75 tons/sq.in. at 2×10^6 cycles and 12·5 tons/sq.in. at 10^5 cycles.

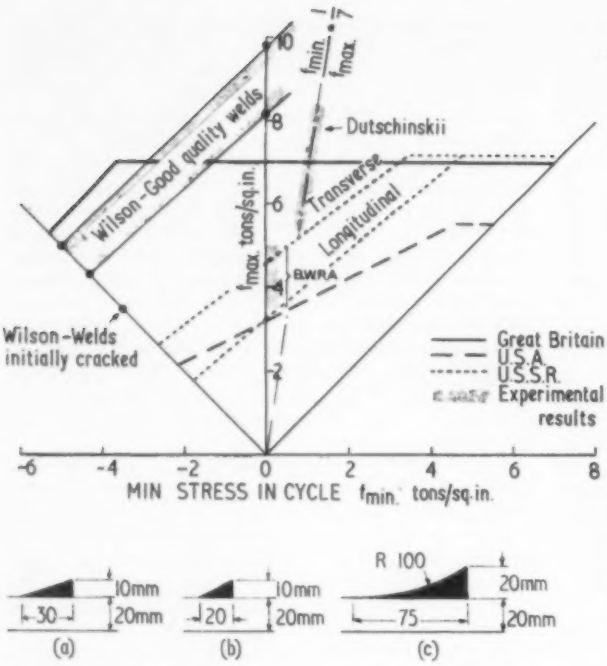
Transverse welds have been studied by Dutschinskii,[12] who tested specimens of Type G but with the welds machined to a triangular profile, as shown in Fig. 6*a*. He recorded strengths of ±3·8 tons/sq.in. on an alternating cycle, 1·0–7·2 tons/sq.in. for $f_{min}/f_{max}=1/7$, and 5·4–10·8 tons/sq.in. for a half tensile cycle. In Germany, Wintergerst and Rückerl[5] again obtained rather high results when they carried out tests on specimens of Type F. Their recorded fatigue strengths were ±4·6 tons/sq.in. on an alternating cycle, and 6·7 tons/sq.in. for pulsating tension. Using the same type of specimen Soete[11] obtained fatigue strengths varying from 4·5 to 6·0 tons/sq.in. in pulsating tension. However, there is not really sufficient evidence available to be too dogmatic about the strength of this type of joint.

Except in the case of the Russian specification the permissible design stresses for load-carrying welds are, rather surprisingly, the same as for non-load-carrying welds. In view of the experimental results already recorded it would seem logical that the design stresses for load-carrying fillet welded joints should be lower than those for non-load-carrying welds. Although it would appear at first sight that current European design stresses for load-carrying joints are dangerously high, it must be remembered that, as far as railway bridges are concerned, design is based on the worst combination of forces, and this is in fact unlikely to occur very often. But it would seem that a review of stress reduction factors for load-carrying welds is urgently required for use in the design of structures which do not have this extra safeguard, and reference to Fig. 5 shows that this review is required even at short endurance levels.

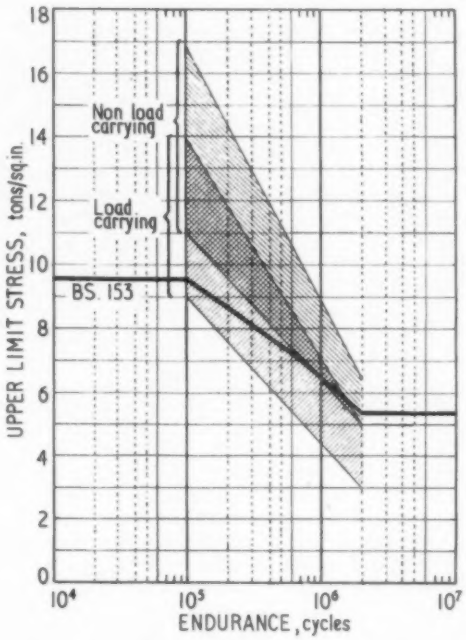
Table IV

Fatigue strengths of load-carrying fillet welds obtained by Wilson

Details of Specimen	*Fatigue Strength, tons/sq.in.*					
f_{min}/f_{max}	−1		0		0·5	
n	10^5	2×10^6	10^5	2×10^6	10^5	2×10^6
$\frac{5}{16}$ in. longitudinal fillet	±6·7	±4·7	12·1	9·8	12·8–25·6	9·0–18·0
$\frac{1}{2}$ in. longitudinal fillet	±7·4	±4·3				
$\frac{5}{16}$ in. transverse fillet	±7·1	±5·0	13·5	8·1	10·1–20·2	8·5–17·0
$\frac{1}{2}$ in. transverse fillet	±8·0	±6·0	16·4	12·5		
A.7 mild steel plate at end of longitudinal fillet	±5·5	±2·5	9·6	4·3	9·2–18·4	3·6–7·2



6—*Weld profiles of load-carrying transverse fillet welds tested by Dutschinskii*



5—*Comparison of design stresses from BS.153 with experimental results. (Pulsating tension cycle)*

4—*Comparison between permissible stresses in weld metal and experimental results. (Fillet welds in shear: 2×10^6 cycles)*

Failure in the Weld

The only comprehensive programme of work on specimens designed to fail in the weld was that carried out by Wilson[8] at Illinois. He used specimens similar to Types G and H (Fig. 2) to study both longitudinal and transverse fillets of two different sizes, and precautions were taken to ensure freedom from cracks in the welds. The results obtained are summarized in Table IV, where the stresses have been computed on the measured area of the weld throat. For the purposes of comparison, the fatigue strengths are also given for A.7 mild steel plate failing at the end of a longitudinal load-carrying fillet weld.

Table IV shows that the larger transverse fillet weld was slightly superior to the other welds tested, but that there was no very significant difference in strength between the two sizes of longitudinal weld and the smaller transverse weld. Based on the fatigue strength of these latter groups it will be seen that the minimum ratio of weld strength to plate strength at 2×10^6 cycles was 1·72 for an alternating cycle.

In work at BWRA,[9] in which no special precautions were taken in regard to weld quality, some weld failures did occur in tests on specimens of Types H and J (Fig. 2). The lower limit of the scatter band containing these results was at approx. 9·0 tons/sq.in. for 10^5 cycles and 3·5 tons/sq.in. for 2×10^6 cycles of pulsating tension loading. The failures were initiated at weld ends, and where this was at the crater end it was often found that the failures were associated with crater defects, possibly including small cracks. These results underline the danger of such defects. This is further confirmed by the results obtained by Wilson, in a preliminary test series which also contained initial weld cracks. The fatigue strengths recorded were ±5 tons/sq.in. at 10^5 cycles and ±3·5 tons/sq.in. at 2×10^6 cycles.

Lower fatigue strengths than those obtained by Wilson have also been obtained by Dutschinskii[13] and by Stallmeyer.[14] Using specimens of Type G with various weld profiles, Dutschinskii obtained fatigue strengths varying between 5·4 and 8·4 tons/sq.in. on a stress cycle having $f_{min}/f_{max}=1/7$ (*i.e.*, stress ranges of 4·6–7·2 tons/sq.in.) based on the weld throat area. Stallmeyer used specimens of Type F and obtained a fatigue strength of approximately 0·9–5·4 (*i.e.*, a range of 4·5) tons/sq.in., based on the weld throat area, but in this case the weld did not extend over the full plate width so that the result may be artificially low because of the extra stress concentration effect.

The permissible stresses in load-carrying fillet welds, as specified in the British, American, and Russian standards, are shown in Fig. 4. The 'cut-off' in the region of alternating stresses in BS. 153 occurs because of the stipulation that the weld throat area must not be less than half the cross-sectional area of the member, and the diagram as drawn assumes that the member involved is designed up to the limit of permissible

stress. Thus, the stress in the weld allowed by BS. 153 may, in some circumstances, be a little optimistic.

Factors Affecting Fatigue Strength

Stress concentration effects

There is no doubt that the major factor affecting the fatigue strength of a fillet welded joint is the stress concentration occuring at the end of a longitudinal weld or at the toe of a transverse weld. This in turn depends both on the shape of the individual weld and on the overall geometry of the joint, and these two factors will be considered separately.

Considering firstly the shape of the weld, it is possible to modify the stress concentration factor by machining the transition point between the weld and the parent metal. Using specimens of Type G (Fig. 2) containing load-carrying transverse welds Dutschinskii[12] obtained a 100% increase of strength at 2×10^6 cycles by grinding the welds to a concave profile, as compared with welds machined to a triangular profile. The three types of weld that he tested, all on the stress cycle $f_{min}/f_{max}=1/7$, are shown in Fig. 6. The two types of triangular weld gave a fatigue strength of approx. 1·0–7·0 tons/sq.in. (*i.e.*, a stress range of 6·0 tons/sq.in.) but the specimens with a weld of concave profile had a fatigue strength of 2·0–14·0 tons/sq.in. But Fig. 6 shows that, to obtain the concave shape, a weld of very large leg length was used, and this somewhat detracts from the practical usefulness of the result. Of more immediate interest is the fact that the elongation of the triangular shaped weld did not result in any significant increase of fatigue strength. Wilson[15] used a similar type of specimen in a short exploratory test programme on the same subject, and he reported a significant increase in strength by using ogee welds instead of 45° fillets.

For non-load-carrying transverse welds Folkhard[7] obtained an increase of about 10% in the fatigue strength on a pulsating tension cycle, of specimens of both Types A and B, (Fig. 1) by machining the welds. On the other hand Bykov,[16] in tests on low-alloy steel specimens, obtained identical results for welds of both convex and concave profile, although in this case the different weld shapes were produced by changing the welding conditions rather than by machining the weld. It was shown earlier that the fatigue strengths obtained for transverse welds have tended to be variable, and several investigators, particularly Dutschinskii and Gurney, have noted the much greater scatter obtained in tests on transverse welds compared with longitudinal welds. It is almost certain that these variations are a direct result of variations in weld profile, possibly including a slight amount of undercut, at the toe of the weld.

Rather surprisingly few investigations appear to have been made into the possibility of improving the fatigue strength of longitudinal fillet welds by machining their ends to a concave profile. However, it is not unreasonable to assume that the results would be similar to those obtained by Puchner[17] in his tests on specimens with discontinuous longitudinal butt welds, in which he obtained an increase in fatigue strength from 5·1 to 7·0 tons/sq.in. at 2×10^6 cycles by machining the weld ends to a radius of 50 mm. Work at BWRA[1] using specimens of Type C (Fig. 1), has shown that a significant increase in fatigue strength can be obtained by using smaller welds, at least when these are non-load carrying. Specimens with $\frac{5}{16}$ in. fillet welds gave a fatigue strength of 5 tons/sq.in. at 2×10^6 cycles, but identical specimens with $\frac{3}{16}$ in. fillet welds had a fatigue strength of 6·5 tons/sq, in., and this increase was presumably due to the reduced stress concentration effect of the smaller welds.

Turning now to the effect of joint geometry, several investigations have been made on specimens of Types A and B (Fig. 1), with transverse gussets welded to one or both sides of the specimen, but in most cases little difference has been found between them although there have been large variations in strength between different investigations. Folkhard obtained nearly identical results for the two types of specimen tested on a pulsating tension cycle, the specimens with two gussets having a fatigue strength between 10·2 and 11·5 tons/sq.in. whilst those with one gusset had a strength between 10·8 and 13·4 tons/sq.in. These strengths are surprisingly high compared with the results of similar investigations carried out by Dutschinskii[13] and at BWRA[6] both of which also gave similar results for the two types of specimen. Using a stress cycle having $f_{min}/f_{max}=1/7$ Dutschinskii obtained a fatigue strength of 1·2–8·6 (*i.e.*, a range of 7·4) tons/sq.in. for both types, and at BWRA the fatigue strength was found to be 6·5 tons/sq.in. On the other hand, in tests on an alternating load cycle, Wilson[4] showed that the fatigue strength was increased from $\pm5{\cdot}8$ to $\pm8{\cdot}4$ tons/sq.in. when the gusset on one side of the plate was omitted. Direct comparison of all these results is difficult however, for information is not available on the shape of the weld profiles.

Using specimens of Type A (Fig. 1), de Leiris and Dutilleul[3] were able to obtain an approximate relationship between fatigue strength and the ratio thickness of attachments (t_2)/thickness of main plate (t_1), the tests being carried out with two different values of t_1. For $t_2/t_1=\frac{1}{8}$ they obtained a fatigue strength of 8·9 tons/sq.in., but this was reduced to 6·3 tons/sq.in. when $t_2/t_1=1$. Intermediate values of strength were obtained for intermediate values of t_2/t_1. The BWRA result of 6·5 tons/sq.in. was obtained with specimens having $t_2/t_1=\frac{3}{4}$. Unfortunately the value of t_2/t_1 in Folkhard's tests is unknown.

Table V

Results of tests on specimens of Type H

Test Series	*Width of Cover Plate, (w) in.*	*Length of Welds, (l) in.*	*l/w*	*Average Life, × 1000 cycles*
Wilson V	9	4	0·44	43
Wilson U	6	4	0·67	156
Wilson T	4	4	1·0	178
BWRA	3·5	4	1·15	180
Wilson EZ	9	10·5	1·17	195

As far as load-carrying fillet welds are concerned, very little work has been done on the effect of joint geometry on fatigue strength. In an exploratory test programme Wilson[15] used, among others, specimens

Table VI

Properties of high-tensile steels

Investigator	*Ref.*	*Steel*	*Composition, %*					*U.T.S., tons/sq.in.*	*Yield, tons/sq.in.*
			C	*Si*	*Mn*	*S*	*P*		
Gurney	9	BS.968	0·23	0·05	1·52	0·033	0·014	37·2	24·4
Klöppel	19	St.52	0·155	0·38	1·12	0·020	0·024	33·6	22·8
Konishi	10		0·16	0·50	1·20	0·017	0·013	35·6	25·7

similar to Type H (Fig. 2), and varied the width of the cover plates and the length of the welds. Only three specimens of each type were tested, all under a constant stress range of 0–18,000 lb/sq.in. in the cover plates, and the average life of the specimens is shown in Table V. This table also shows the result of some tests carried out at BWRA[9], the life quoted being that corresponding to a stress of 18,000 lb/sq.in. on the S/N curve.

Thus it will be seen that for constant width of cover plate there is a large increase in life with increase in weld length, and also that for constant weld length life increases with decrease in cover plate width. Qualitatively this can presumably by explained by the fact that for small values of l/w the cover plate is not fully effective in transmitting stress at the cross section containing the weld extremities, so that the geometrical stress concentration factor is effectively increased. For design purposes it would seem reasonable to make $(l/w) \geqslant 1$, but the evidence on which this suggestion is based is rather slender.

The remainder of Wilson's exploratory test programme consisted of tests on joints with various arrangements of longitudinal and transverse welds, but none of these was significantly better than a joint similar to Type H (Fig. 2) with the welds continued to form a transverse weld across the back of the cover plate. Tested under a stress range of 0–18,000 lb/sq.in. the average life of three of these specimens was 283,000 cycles.

In tests on specimens consisting of double angle members welded to gusset plates at their ends, Koenigsberger and Green[18] showed that a small improvement in fatigue strength resulted if the welds were proportioned in relation to their distance from the neutral axis of the angle rather than with equal welds on each side of the angle. Unfortunately there is a slight doubt whether this was in fact the reason for the improved strength, since in the case of the proportioned welds the gusset extended slightly in front of the welds and thus provided a stiffening effect on the angles, and this may have been at least partly responsible for the improvement.

High-Tensile Steel

It would be very encouraging if it were possible to record that the use of high-tensile steel led to greater fatigue strengths than those obtained with mild steel, but in every case except one the strengths of mild and high-tensile steel fillet welded joints have been virtually the same. Details of some of the high-tensile steels used in the investigations referred to hereafter are shown in Table VI.

In Britain exploratory tests on specimens of Type C (Fig. 1) fabricated from steel to BS. 968 have produced results equivalent to those obtained with similar specimens made from BS. 15 mild steel. Similarly, a few tests carried out on St. 52 steel of German origin produced results identical with those obtained with BS. 15. In passing, it is of interest to note that this form of specimen seems to be particularly suitable for examining the effect of material differences, because it leads to very consistent results with an almost complete lack of scatter. Unfortunately the same cannot be said of a butt welded joint, which is often used for this purpose.

Comparative tests between mild and high-tensile steel have also been carried out in the form of load-carrying fillet welded joints. In Japan, Konishi[10] has tested specimens of Type H containing longitudinal welds. The cover plates were fabricated from both mild and high-tensile steel using two different electrodes for each. For mild steel cover plates the fatigue strengths were 7·2 and 8·2 tons/sq.in., and for high-tensile steel they were 7·2 and 7·5 tons/sq.in.

The only investigators who have shown any improvement in strength over mild steel were Klöppel and Wiehermüller,[19] who tested specimens fabricated from St. 52 in Germany. Comparison of their results with those obtained by Wintergerst and Rückerl[5] on identical specimens in St. 37 shows an improvement from 13·7 to 15·9 tons/sq.in. for a non-load-carrying joint of Type B and from 6·7 to 7·6 tons/sq. in. for a load carrying joint of Type F, both on a pulsating tension cycle. However, as has been stated earlier, check tests on St. 52 of German origin, using specimens of Type C (Fig. 1), gave results identical with those obtained with BS. 15. In view of this it would seem prudent to treat the German results for St. 52, and, consequently, also the results for St. 37 with considerable reserve. It seems hardly credible that a non-load-carrying joint in Germany can be nearly three times as strong as a similar joint produced anywhere else in the world.

Residual stresses

A good deal of the recent work on this aspect of the subject appears to have been carried out either in Russia or in Britain, although Puchner's[17,20] work in Czechoslovakia on discontinuous longitudinal butt welds is closely allied to it. Until a very short time ago there was considerable doubt whether or not residual welding stresses had any effect on fatigue strength, but in view of the recent work, which is outlined in the following section, it can now be stated definitely that they can have an important effect. The Russian work has had as its main object merely the proof of the fact that residual stresses do affect fatigue strength. On the other hand the work at BWRA has been concerned primarily with the possibility of increasing the fatigue strength of certain types of fillet welded joint by inducing favourable residual stresses.

This work will therefore be considered separately

later, although it has also yielded further information on the basic effect of residual stresses on fatigue strength.

Some of the first Russian work was that by Kudryavtzev,[21] who used a specimen similar to Type C (Fig. 1) and found that, after thermal stress relief, the fatigue strength was raised to ±4·8 tons/sq.in., whereas in the as-welded form it was less than ±2·0 tons/sq.in. To find out how much of this increase of strength was due to the relief of residual stresses and how much to metallurgical changes he normalized the specimens and then welded a second gusset on top of the first, as shown in Fig. 7, so as to reintroduce the residual stresses without producing any metallurgical changes. In the normalized condition the fatigue strength was found to be ±6·0 tons/sq.in. but, after the second gussets had been welded on, it was again found to be less than ±2·0 tons/sq.in. This shows that, in this case, the removal of residual stresses resulted in an increase of fatigue strength of between 100 and 200%. Too much attention should not, perhaps, be paid to the actual strength values recorded in this investigation, because the number of specimens tested was small. Nevertheless the large increase in strength is convincing, and it was the first investigation to demonstrate the importance of residual tensile stresses.

Navrotskii[22] used specimens of Type J (Fig. 2) with load carrying fillet welds, and carried out fatigue tests with three different distributions of residual stress. All the specimens were stress relieved and one series was tested in that state. The other two series had new residual stress systems created in them, the first by heating the edges of the central plate, which resulted in residual compression at the notch, and the second by cold forging the edges so as to produce residual tensile stresses at the notch. The residual stresses at the notch were calculated to be approx. −9·7 and +8·1 tons/sq.in. respectively, due to the two treatments. The fatigue strengths were found to be ±2·5 tons/sq.in. when no residual stresses were present, and this was increased to ±3·4 tons/sq.in. after heating and reduced to ±1·8 tons/sq.in. after cold forging. Since in this investigation the areas affected by the residual stresses and by local plastic deformations, which may also affect fatigue strength, were separated, the change of fatigue strength can definitely be ascribed to the effect of the residual stresses.

Conclusions

From the experimental work and design specifications reviewed in the foregoing the following general conclusions can be drawn:

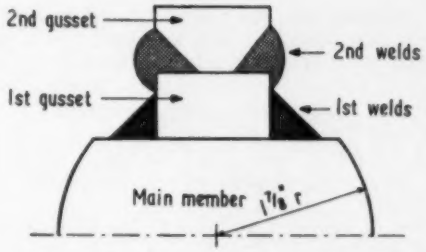
(1) All fillet welded joints have relatively poor fatigue strengths, but load-carrying welds are considerably worse than non-load-carrying welds. Current European design specifications provide no factor of safety against failure at 2×10^6 cycles for members with non-load-carrying welded joints, whilst for members containing load-carrying fillet welded joints design stresses are actually greater than fatigue strengths determined experimentally.

(2) As for the welds themselves, load-carrying fillet welds designed in accordance with BS. 153 should prove satisfactory when subjected to load cycles between $f_{min}/f_{max}=-0{\cdot}2$ and $+1{\cdot}0$, but for load cycles with larger compressive components the experimental evidence suggests that a reduction of design stresses might be desirable. If the weld contains an initial crack, however, this may propagate in fatigue at quite low stresses.

(3) The fatigue strength of a fillet welded joint depends to a large extent on the stress concentration effect, which in turn depends both on the shape of the weld profile and on the geometry of the joint. Thus, there is some evidence that transverse welds can give higher fatigue strengths than longitudinal welds, but, presumably owing to differences in weld profile, possibly with some undercutting, results for transverse welds tend to be very scattered, whilst the behaviour of longitudinal welds is very consistent. Small improvements in strength can be obtained by machining welds, but it seems doubtful if this is an economic proposition taking into account the smallness of benefit obtainable.

(4) All tests on high-tensile steel joints have given virtually the same fatigue strengths as for similar joints in mild steel.

(5) Residual stresses exert some influence on the fatigue strength of fillet welded joints, and in fact their use provides the only obvious possible method of producing significant increases in strength. This aspect of the problem has recently received special study at BWRA and the work to date is reviewed in the following section.



7—Part cross section of specimen used by Kudryavtsev

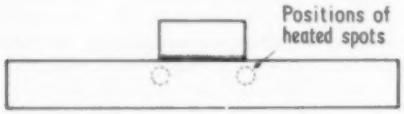
Improving the Fatigue Strength of Fillet Welded Joints by Induced Residual Stresses

Work on this subject originated in Czechoslovakia, where Puchner investigated the fatigue strength of specimens with discontinuous longitudinal butt welds, and in particular the effect of inducing residual stresses at the weld ends by local heating with an oxy-acetylene torch. This process and those similar to it, such as local heating using a resistance welding machine, have been termed 'spot heating'. It must be emphasized that it is *not* a method of stress relieving but of inducing favourable residual stresses. By this means Puchner obtained a spectacular increase in fatigue strength under pulsating tension loading, the actual fatigue strength values at 2×10^6 cycles being 5 tons/sq.in. for as-welded specimens and 12·7 tons/sq.in. for spot-heated specimens of the form shown in Fig. 8. Exploratory work at BWRA[23] on a similar type of specimen confirmed Puchner's results, and it

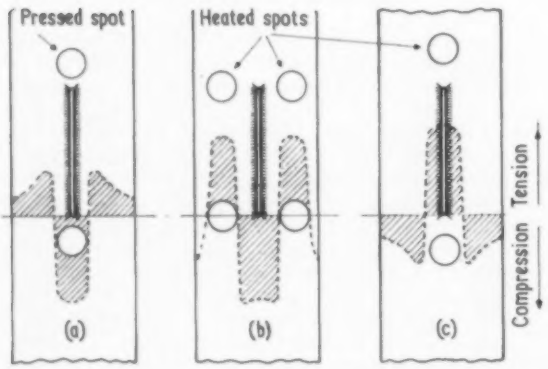
was apparent that the method could be applied to fillet welded joints. It also appeared that a suitable residual stress system could be induced by mechanical means by compressing the specimen between dies, and since this method was more susceptible to control than spot heating it has been extensively used in the BWRA investigations. The processes of 'spot heating' and 'local compression' will be compared in more detail later.

Most of the work at BWRA[1] has been carried out using specimens of Type C (Fig. 1), on which a very thorough investigation of fatigue properties has been made. Specimens were tested under four load cycles, $f_{min}/f_{max} = \frac{1}{2}$, 0, −1 and −4, and with three different residual stress distributions, as-welded, stress relieved, and locally pressed between dies in the position shown in Fig. 9*a* so as to introduce residual compressive stress at the notch. The results obtained are shown in the form of a Goodman diagram in Fig. 10. From this, the beneficial effect of both stress relieving and particularly of the local compression treatment can be clearly seen. In this diagram the general form of the curve for stress-relieved specimens, which has been confirmed by Puchner[20] is of particular importance, since the increase in the stress range required to cause failure as the minimum stress in the cycle becomes more compressive explains why residual compressive stresses induced at the notch increase the fatigue strength, while residual tensile stresses, such as those due to welding, reduce it.

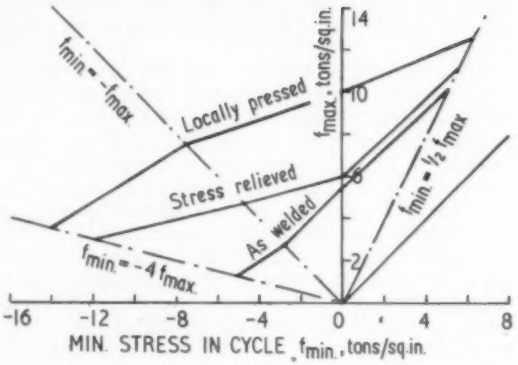
It has been suggested that the beneficial effects of spot heating were in fact not due to the induced residual stresses but to metallurgical changes caused by the heating. However, the improvement in strength obtained by local compression treatment tends to show that the result is due to residual stresses, and this has been further confirmed in other ways.



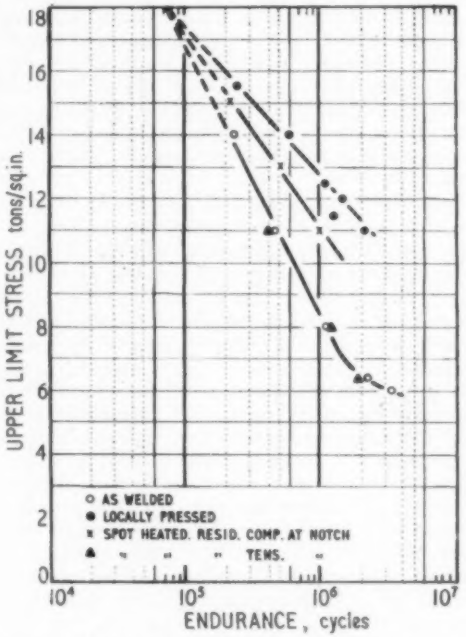
8—*Form of specimen used by Puchner*



9—*Positions of post-weld treatments used in BWRA experiments and resulting qualitative residual stress distributions*



10—*Fatigue test results for specimens of Type C with various residual stress distributions*



11—*Fatigue test results for specimens with various residual stress distributions*

In the first place it was noted that the S/N curves for test series with any residual stress system, but subjected to any one particular type of load cycle, tended to converge at the yield stress of the material. If the change in fatigue strength were due to residual stresses this was to be expected since, under an applied stress of yield, 'mechanical stress relief' results and residual stresses have no effect. In addition, tests have also been carried out on two series of specimens of Type C which were spot heated in different positions, as shown in Fig. 9*b* and *c*, so that one series had residual compressive stresses introduced at the notch and the other had residual tensile stresses there. However, all the heated spots were positioned at the same distance from the notch so that metallurgical effects would be constant between the two series. The results

are shown in Fig. 11, and demonstrate that specimens having residual tensile stresses induced at the notch gave the same fatigue strength as 'as-welded' specimens, whilst the series having induced residual compressive stresses showed an improvement in fatigue strength. Incidentally, the tendency of the various S/N curves to converge at the yield stress of the material, 18 tons/sq.in., can be clearly seen in this diagram. In view of these results it is considered that the increase in fatigue strength obtainable by spot heating and local compression can definitely be ascribed to the effect of residual stresses.

There are still many problems to be solved before these techniques can be generally applied in practice. For instance, it has been suggested that the beneficial effect may not persist, and that there may be deterioration in the mechanical properties of the heated materials. It must also be realized that nearly all the work has been carried out on non-load-carrying joints in mild steel, although work currently in progress has produced encouraging results for load-carrying joints in mild steel and for non-load-carrying joints in high-tensile steel. In fact, there is good reason to believe that results for high-tensile steel should be better than those for mild steel. These methods could not, of course, be applied to structures liable to overstressing, since this would tend to eliminate the residual stresses.

However, one of the main difficulties lies in the design of joints suitable for treatment. It is essential that 'notches' should be localized—*i.e.*, welds from which failure may start must run longitudinally, parallel to the direction of stress, and not transversely—so that the region of induced residual compressive stress can embrace the notch without any of the balancing region of residual tensile stress also acting on it. At the present time this condition is not often fulfilled in actual joints used in welded fabrications, and even when notches are localized a joint seldom consists of a single notch on a plate member such that treatment would involve a comparatively simple residual stress distribution. Defining the positions for spot heating, even in simple cases, must at the moment be done by intuition, and it is almost impossible to define the positions for more complicated joints. Before this can be done with any certainty it will be necessary to determine a method for calculating residual stress distributions, due to one or more heated spots, although in the interim period the experimental measurements of residual stress caused by a heated spot carried out by Norton and Rosenthal[24] give some guidance on the subject.

In this respect the technique of local compression offers some advantage over spot heating because there is no difficulty in defining the correct position of the treated spot relative to the notch. It also has the advantage that the process is easier to control, in that the position of treatment can afterwards be seen, and the fact that the correct load was applied can be checked by measuring the residual indentation. On the other hand, the means of applying the load, particularly on site, may be more difficult than spot heating with an oxy-acetylene torch, but the development of a suitable hydraulic press or even an explosive method should not be too difficult. However, it is believed that both methods will prove useful, in different circumstances, for the treatment of fillet welded joints.

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Influence of Weld Details on Fatigue of Welded Beams and Girders

By W. H. Munse
and J. E. Stallmeyer

A discussion is presented of the effect of various details on the fatigue resistance of welded beams. Included are such details as splices, stiffeners, cover plates, and attachments, all of which can be expected to produce a reduction in the fatigue strength of the basic beam.

Introduction

In recent years there has been a marked increase in the use of welding for many types of structure. With the increased applications of welding to bridges and other similar structures subjected to repeated loadings, fatigue has become of greater importance. This is a result of the stress concentrations associated with many of the details inherent in welded construction.

The fact that bridge engineers are aware of some of these fatigue limitations is obvious in the current design specifications. Nevertheless, even in bridges the fatigue problems differ markedly for the various component members, connections, and details. Some members receive relatively few applications of maximum load during their life, whereas others may receive relatively large numbers of maximum loadings. In a similar manner, some members may be subjected to relatively small changes in stress whereas others may receive reversals or large ranges of stress during a loading. All of these factors are of importance and should be taken into account in design if these structures are to resist properly the applied loads.

Another important factor in structures subjected to repeated loadings is the geometry of the details of the individual parts; the resulting stress concentrations combined with repeated loadings may produce failures unless proper provision for them is made in design.

One type of bridge member in which welding is widely used is the welded **I** beam. Such members are easily fabricated and are often very economical; in their fabrication there can be a great variety of details. But, unless properly used, these details can affect markedly the fatigue resistance of the members.

In this paper an analysis will be made of the fatigue behaviour of various flexural members to illustrate the effects of the fabrication details on fatigue resistance. In addition, the fatigue behaviour will be related to the fatigue strength of simple beams and plates. The information thus obtained should serve as a guide and be of assistance in estimating the effects that similar details will have upon the fatigue behaviour of welded flexural members.

An attempt will be made also to relate the test behaviour to actual service conditions, although most tests have been made on loading cycles of zero-to-maximum and not actual service cycles. Because of the many possible combinations of loadings found in the field, only limited data can be developed in the laboratory to cover directly service conditions. Nevertheless, interpolations and extrapolations from existing data should provide reasonable indications of what can be expected under actual service conditions.

Materials and Tests

The results from a number of fatigue tests will be utilized in this study to illustrate the observations that are presented. However, the evaluation will be restricted to members of structural grade steels generally classified under ASTM Specifications A-7 and A-373. These are the principal materials that have been employed in the United States for welded bridges and structures. The chemical and physical properties of the A-373 steels upon which much of this discussion is based are presented in Table I. This steel is very similar to that used in many of the previous tests in both the United States and in Europe.

Because of the limitations of space, complete details of the specimens and tests referred to are not given; they may be obtained in the original references. However, a general indication of some of the principal parameters will be presented so that the reader will be acquainted with the various factors being studied.

Testing machine capacities have limited to some extent the size of the members that could be tested. Nevertheless, the machines have been large enough to permit the testing of beams with depths as great as 16 in. and with spans of 8–9 ft. Although these members are relatively small in comparison with long-span

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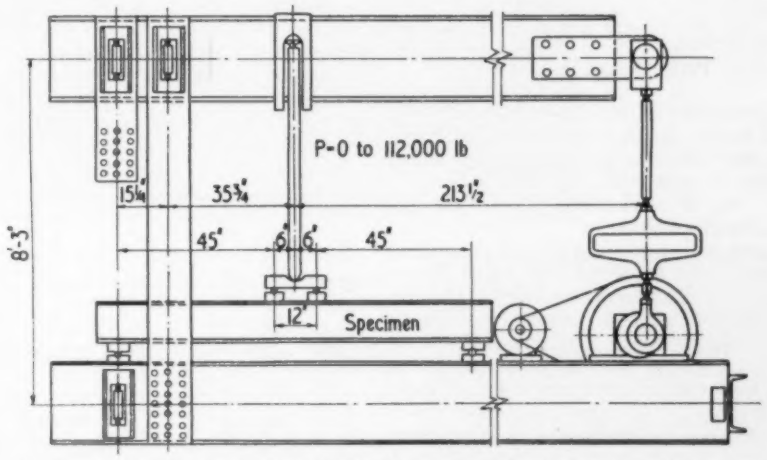
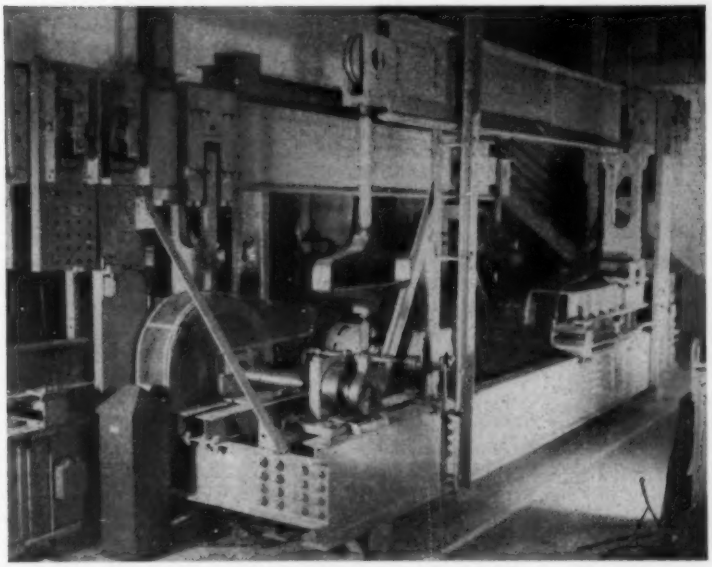
Table 1
Properties of ASTM A-373* steel plates for welded beams

Thickness, in.	*Composition, %*						*Yield Strength, lb/sq.in.*	*Ultimate Strength, lb/sq.in.*	*Elong. on 8 in., %*	*Red. in area, %*
	C	*Mn*	*P*	*S*	*Si*	*Cu*				
$\frac{3}{16}$	0·23	0·63	0·022	0·031	0·030	0·17	38,800	64,800	29·6	58·0
1	0·21	0·60	0·030	0·030	0·053	0·20	34,600	67,000	28·0	48·6

* ASTM A-373 structural grade steel for welded bridges.



1—*Illinois fatigue testing machine, 200,000-lb capacity, adapted to test flexural specimens*



2—*Flexural fatigue tests in progress in 200,000-lb Illinois fatigue machines*

welded bridges, they are of such a scale that the results of the tests should be applicable to the design of full-size structures.

In the fabrication of the various members, typical structural welding procedures have been used to produce what would be considered welds of sound quality. Many of the details utilized in the tests have been of limited size but are still representative of the larger details used in practice.

Most of the fatigue tests mentioned were conducted in the 200,000-lb fatigue testing machines shown in Figs. 1 and 2. These machines, when used for the testing of welded beams, are capable of applying a maximum mid-span load of approximately 112,000 lb

(50 tons) at a rate of 180 applications per minute. Thus, approximately 250,000 cycles of loading can be applied in one day or 2,000,000 cycles of loading in approximately eight days. To date, these machines have been used only for tests on a zero-to-tension or a partial-tension to a full-tension loading cycle in the tension flange. However, other investigators have occasionally employed equipment capable of producing reversed-load testing cycles. Thus, a variety of testing cycles have been used in the various fatigue tests on welded flexural members.

Plain Beams

Only a limited number of tests have been conducted on plain rolled beams. In one investigation,[2] tests on 12-in. **I** beams provided a fatigue strength for failure at 2×10^6 cycles of approximately 31,200 lb/sq.in. (14 tons/sq.in.). At 10^5 cycles this fatigue strength would be considerably higher.

It is of interest to compare the fatigue resistance of plain rolled beams with that of flat plates in the as-rolled condition. A summary of the fatigue strengths of these two types of members is presented in Table II; the fatigue resistance of the rolled beams is only slightly lower than that of the plain flat plate. The reason for this difference is not known; but it might be surmized that it may result from residual stresses in the beams, or possibly the strain gradient that exists in a beam subjected to flexural type loadings. Regardless of the reasons for this difference, the decrease in fatigue strength is obviously relatively small.

Table II

Fatigue strengths of plain plates and plain beams

(Zero-to-tension cycle)

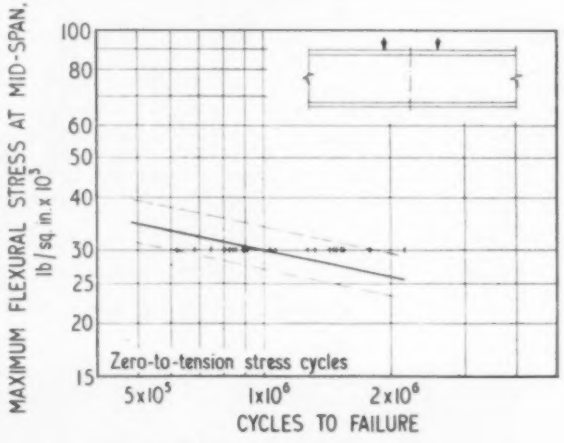
Type of Member	*Fatigue strength, lb/sq.in.* 2×10^6 *cycles*		10^5 *cycles*
	A-373	*A*-7	*A*-7
Plain Plate	33,000	31,700	47,800
Rolled Beam 12131·8[2]	—	31,200	—

By extrapolation from the beam strength of Table II and the data for plain flat plates of A-7 steel, the fatigue strength of a rolled beam at 10^5 cycles might be predicted to be approx. 47,000 lb/sq.in. (21 tons/sq.in.). Because of the magnitude of this stress level, special precautions would be necessary to prevent buckling and the effects of yielding if the value were to be established experimentally. However, since the actual data are not available, the approximate value suggested will be used in this analysis.

Welded Beams

A number of tests[1,5] have been made on plain welded beams, which have varied somewhat in detail. Although considerable variation has been obtained in the fatigue lives of these members, their average fatigue strength for failure at 2×10^6 cycles is approximately 26,000 lb/sq.in. (11½ tons/sq.in.). This is somewhat lower than the fatigue resistance obtained for plain rolled beams. Some of this decrease, no doubt, results from the details which are inherent in the fabricated beams, several of which will have an adverse effect on the fatigue resistance of the members. These include such factors as the weld and its geometry, the edge preparation of the various plates, the relative thickness of the various components, and so on.

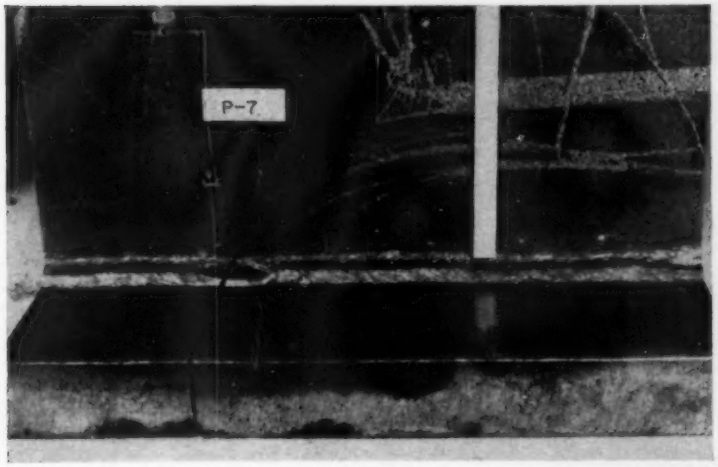
As a result of variations in these factors and the scatter generally obtained in fatigue test results, the fatigue lives of the various welded beams tested at one given stress cycle were found to vary from approx 6×10^5 to 2×10^6 cycles (*see* Fig. 3). This scatter, although large in terms of fatigue life, is equivalent to a variation in fatigue strength of only about 7,000 lb/sq.in.



3—*Fatigue data for plain welded* **I***-beams*

One of the factors that has been studied and found to have an effect upon the fatigue resistance of the welded beams is the ratio of the thickness of the flange plates to that of the web.[5] In one series of tests this ratio was varied from 1·0 to 5·3. Although limited in number, the tests, all conducted on the same stress cycle, gave lives ranging from 830,000 to 2,164,300 cycles. With so few tests, one would normally hesitate to draw conclusions concerning the effect of the flange-web thickness ratio, but the trend has been consistent in all tests of the series and consequently the effect appears to be significant.

In addition to the variation in fatigue life, another feature observed in the tests of specimens with various ratios of flange-to-web thickness was a difference in mode of failure. The point of initiation was always in the fillet welds at the web-flange junction; but the sequence and direction of crack propagation differed for the specimens with various flange-to-web thickness ratios. For members with low ratios (1:1) the cracks generally propagated through the flanges first and then into the web of the beam. For specimens with high flange-to-web thickness ratios, the initial direction of propagation was into the web. In this instance the rate of crack propagation was somewhat lower because of the continual reduction of nominal stress at the crack front as it propagated toward the neutral axis. When

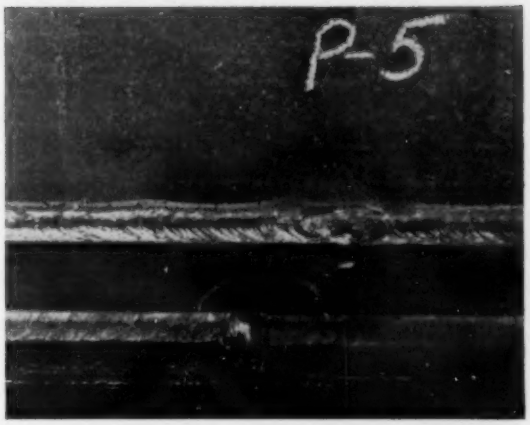


4—Fatigue crack initiated at weld crater

the crack in the web was 2–3 in. long it then began to spread into the flange. Therefore, for the specimens with thin webs, a larger number of cycles was necessary to complete the failure after a visible crack had appeared than for specimens with thick webs. This helps to explain, at least in part, the variation in fatigue life obtained for the members with the various ratios of flange-to-web thickness.

Most of the plain welded beams failed in the pure-moment region of the members, with the cracks initiating at the weld craters in the web-flange junction. However, one of the failures initiated at a section a short distance outside the pure-moment area and, after propagating into the flange, travelled upward in the web at an angle. This change in direction of the fatigue crack was a result of the combined state of stress existing in the web material; a view of this fracture is shown in Fig. 4. The photograph also shows the initiation of the fatigue crack in the weld crater resulting from a change in the welding electrode. In most of the other members of this type, wherein failure occurred in the pure-moment region, the crack in the web propagated vertically.

In another instance, the fatigue failure initiated at the edge of one flange plate. However, upon examination it was found that a sizable irregularity existed in the flame-cut edge of the plate and provided the starting point for the crack (Fig. 5). Thus, fabrication details too can be responsible for defects which might initiate fatigue failures. However, although such details may provide the point at which the failures initiate, their effect on the fatigue strength of the member may not be much greater than the effects of some of the weld details.



5—Fatigue crack initiated at flame-cut notch in flange

Spliced Beams

Splices are often necessary in welded girder bridges, particularly in those which are parts of continuous structures. The details of such splices may be of several types; with the welds all in a single plane or staggered, and either with or without cope holes. In addition, shop splices may be made in either the web or flange alone. All of these details, because of their inherent stress concentrations, can be expected to have an effect on the fatigue resistance of the beams. Consequently, a consideration of their effect on the fatigue behaviour of a welded beam follows.

Data are available from a limited number of tests on the splices shown in Fig. 6. These include the several splice details already noted and also a member with a web cope hole alone.

The results of fatigue tests on members with four of the splices are presented in Fig. 7. The slopes of the S/N curves vary for the two basic types of splice; the curves are steeper for the members with the splice in a single line than for those that are staggered. It may be noted also that the cope hole reduced the fatigue strength in both instances by 2000–3000 lb/sq.in. Thus, both the splice and the details of the splice affect the fatigue behaviour of the member.

For the thin web members shown in Fig. 6 ($\frac{3}{16}$-in. web), it is possible for the welder to produce a good quality flange weld without the use of a cope hole. However, for a member with a greater web thickness it might be necessary to use the cope hole to obtain a sound flange weld. Consequently, care must be

exercised in extrapolating from the laboratory tests to the conditions in actual structures; the details which prove to be best in the laboratory may not be as effective in the field.

A summary of the fatigue strengths at 2×10^6 cycles for the various spliced members is presented in Table III. It is of interest to note that the unit fatigue strength

Table III

Summary of fatigue strengths for spliced members
(Zero-to-tension cycle)

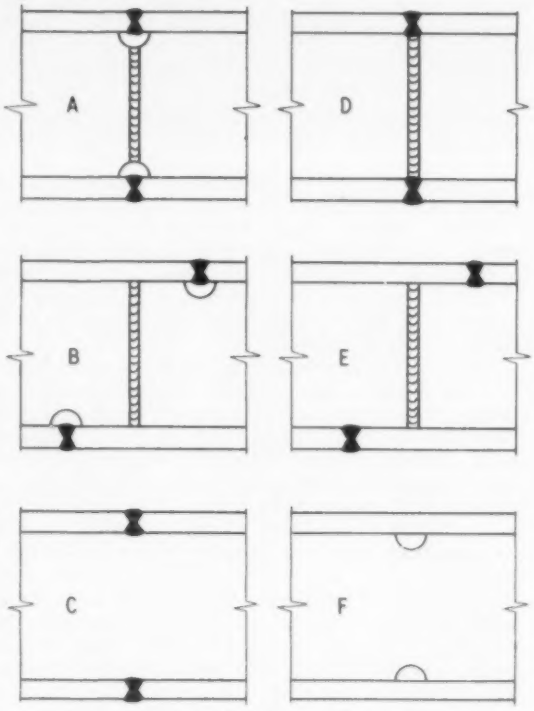
Member	*Fatigue Strength, lb/sq.in. at 2×10^6 cycles*
Plain plate with tranverse butt welded splice (as welded)	23,800
In-line splice with cope holes (type *A*)	17,500
Staggered splice with cope holes (type *B*)	21,000
In-line splice (type *D*)	19,000
Staggered splice (type E)	23,000

of these spliced beams can be made to approach that of a plain plate with a transverse butt weld.[4] In addition, it may be seen that the fatigue strength of the staggered splices was not greatly different from that of the plain welded beam; the strength of the spliced beam was only 3000–8500 lb/sq.in. below that of the original section.

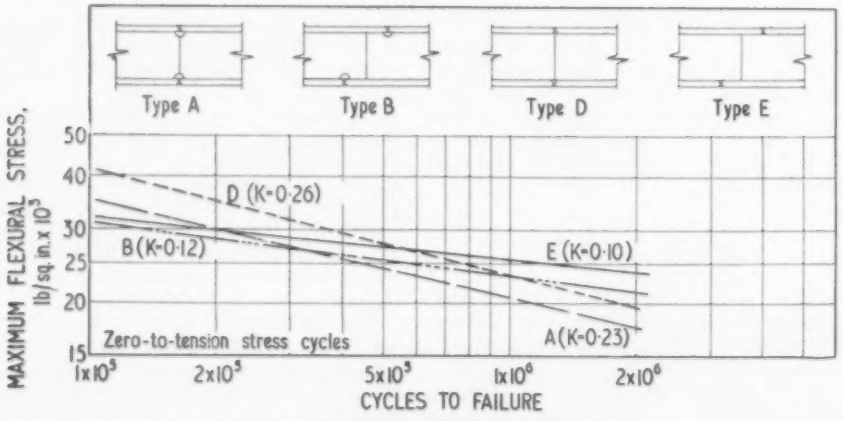
Only three tests were conducted on the type *C* splice detail. Nevertheless, as would be expected, the fatigue behaviour of these members was quite similar to that obtained in the tests of plain flat plates with transverse butt welds. Thus, the full-depth splice gives a slightly lower fatigue resistance than the simple shop-type flange splice alone. This difference may be a result of the difference in residual stresses in the field-type full-depth splice or possibly from the compounding effect of the stress concentrations resulting from both the web and flange splices.

Further information can be obtained concerning the fatigue behaviour of the spliced members from an examination of the fractures of these members. Although the number of tests was limited, a variety of failures has been obtained. Some occurred in the weld (Fig. 8) while others initiated at the toe of the butt weld (Fig. 9); but the fatigue resistance of these various members was not greatly different.

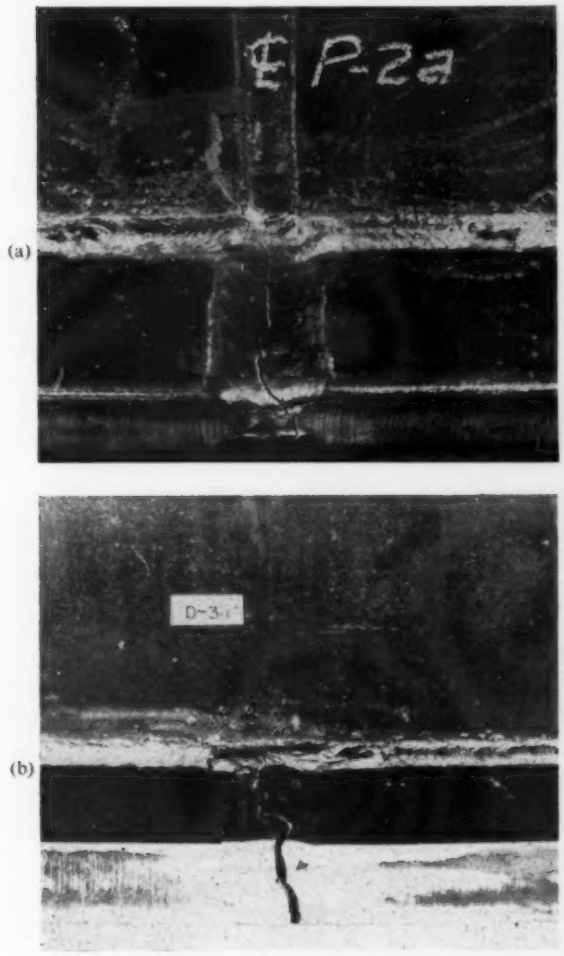
In one instance, three fatigue cracks were obtained in the flexural member which was fabricated with a staggered splice. This member, shown in Fig. 10, developed initially a fatigue crack in the compression flange. Since this crack did not impair the flexural



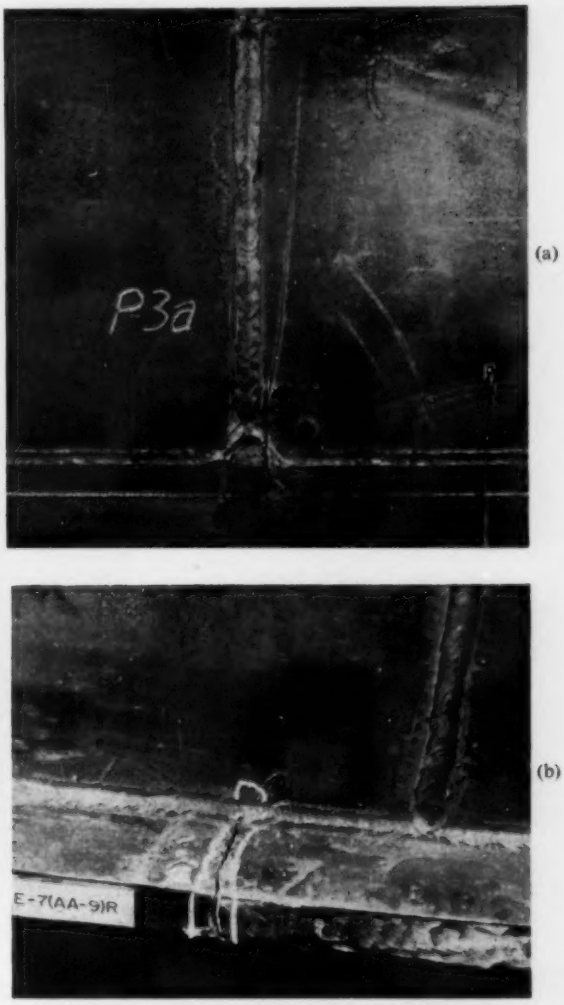
6—Details of splices in welded beams



7—Comparison of test results for spliced beams



8—*Flange-weld fatigue cracks in beams with butt-welded splices:* (a) *In-line splice;* (b) *staggered splice*



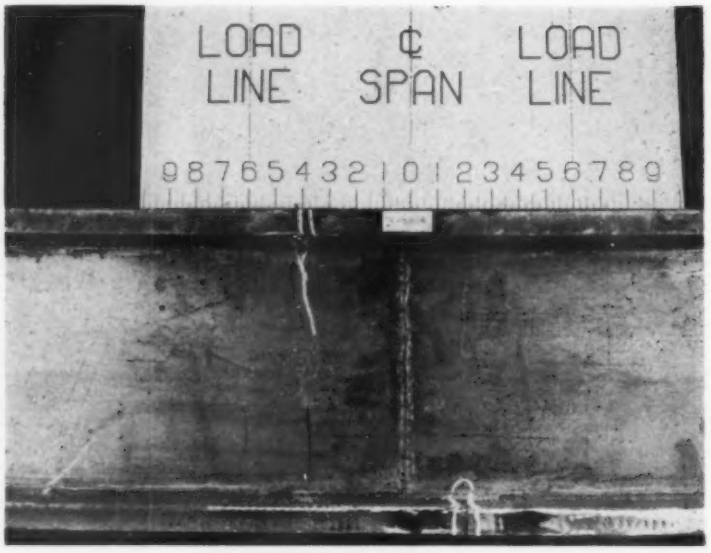
9—*Fatigue cracks at edge of flange welds in beams with butt-welded splices:* (a) *In-line splice;* (b) *staggered splice*

capacity of the member, the test was continued and cracks later developed in the pure-moment section of the tension flange and also in the web at a point a short distance outside this region. Thus cracks developed at three locations in this single member and in regions where the nominal conditions of stressing varied considerably.

In general, it is believed necessary to have tensile stresses to develop and open a fatigue crack. Consequently, the fatigue crack in the compression flange of the member in Fig. 10 was unexpected. However, in this member, the residual tensile stresses in the transverse flange weld could provide the tensile stress necessary to develop, in combination with the applied compressive stress, the stress conditions which may have initiated the crack. The actual stress at the weld in the compression flange may thus vary from a residual tension to a compression.

When cope holes were employed with the beam splices, several additional types of fatigue crack were found. These are shown in Figs. 11 and 12. In all members with cope holes the stress concentrations at the toe of the fillet welds or at other notches in the cope hole appear to have been responsible for the initiation of the fatigue cracks. Apparently the fatigue failures of the spliced members with copes always occur at these copes and at the points of maximum stress concentration.

Although the evaluation presented so far for beam splices has been made on the basis of the conditions necessary to produce failure, it should be realized that the fatigue resistance of the members was relatively great and that high stresses had to be applied to obtain the failures. At 2×10^6 cycles, the lowest fatigue strength of the splices was approx. 70% of the fatigue resistance of the plain welded beam; in other instances, this ratio approached 90%. Consequently, with sound welds, proper details, and suitable procedures, excellent fatigue resistance can be built into welded beams



10—*Welded beam with fatigue cracks in web, tension, and compression flanges*

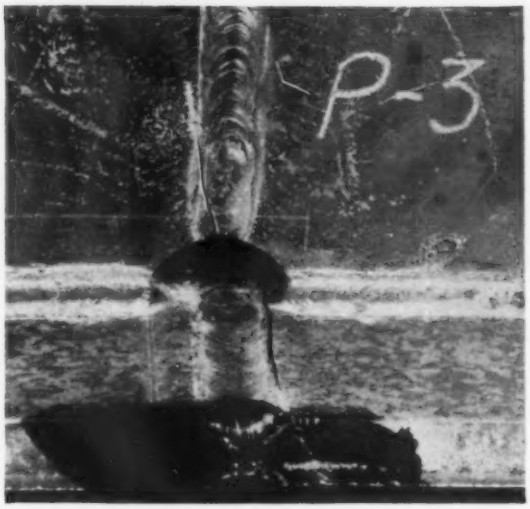
even when they must be spliced. However, poor details or bad welds can be expected to reduce significantly the fatigue resistance of any of the splice configurations discussed.

Stiffeners

The principal American Specification for Welded Highway and Railway Bridges[6] permits the ends of stiffeners and other attachments to be welded to flanges only at points where the flanges carry compressive stress or where the tensile stress does not exceed 75% of the maximum allowable stress permitted by the applicable general specification. No consideration is given in the specifications to the effect of the stiffeners on the fatigue resistance of the web or of the effect of the shear in the member upon its fatigue strength. However, the results of recent tests have suggested that both these factors may need consideration.

Again, as in the previous discussion, the results of some of the recent tests conducted at the University of Illinois will be used to demonstrate the effects of stiffeners on the fatigue behaviour of welded beams. A variety of stiffener details have been studied; although these are but a few of the many details that are possible, they give a general picture of the effect of stiffeners on the flexural behaviour of the members.

Six types of stiffeners have been studied; the general details of these stiffeners, designated as types *A* to *F*, are shown in Fig. 13. Type *A* permitted no welding to the tension flange and can be compared with type *B* which is the same detail except that the stiffener is welded to the tension flange, as well as the web and the compression flange. The type *C* stiffener was attached only to the web by intermittent welds. All three of these details provide for stiffeners on one side of the web. The type *D* stiffener is the only one of the group which employed stiffeners on both sides; in all other respects it was comparable with the type *A* stiffener. The fifth stiffener detail, type *E*, was also similar to type *A*, except that the web weld was held



11—*Fatigue crack at toe of cope-hole fillet*

2 in. away from the tension flange. These various stiffeners were selected to study the effect of welding to the web, to evaluate the use of stiffeners on one or both sides of the web, and to determine the effect of welding to the flange.

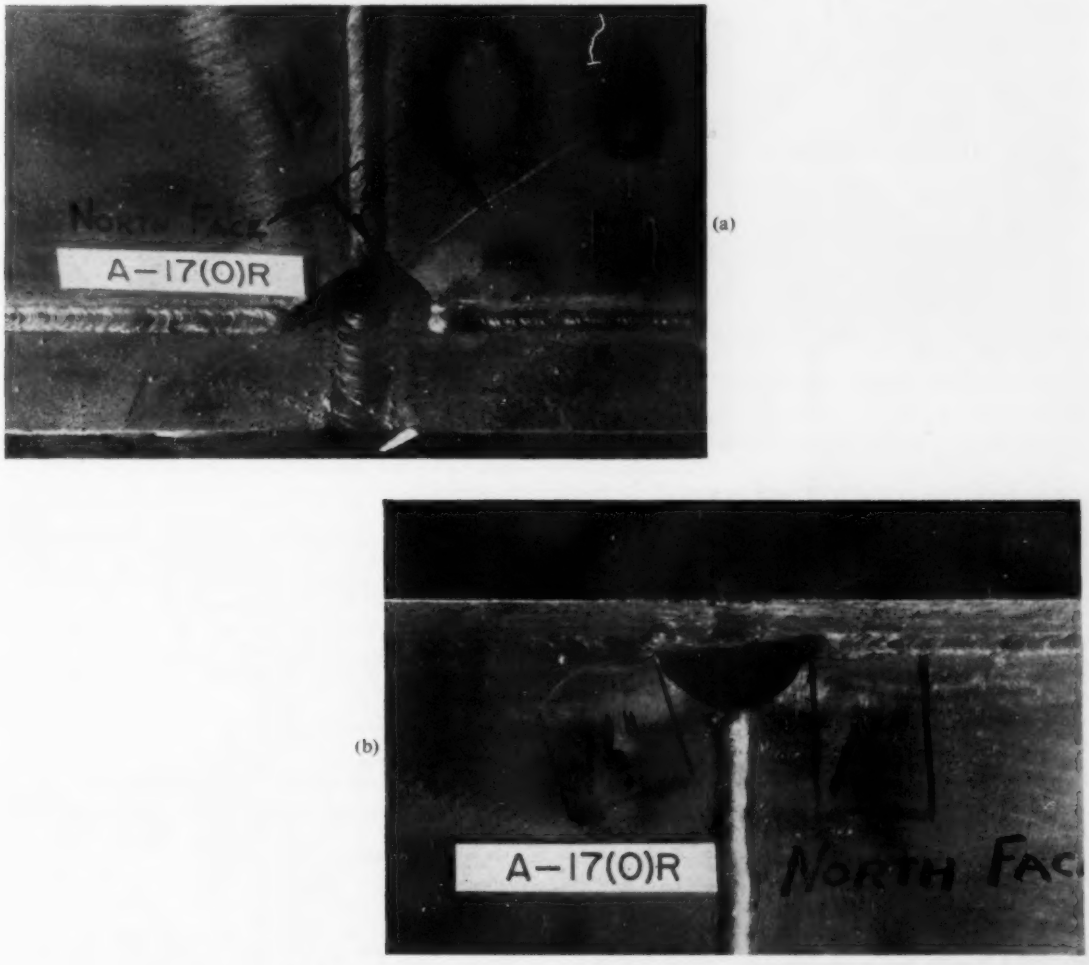
Tests on beams with stiffeners were conducted on stress cycles of zero-to-tension in the bottom flange. The results of these tests are summarized in the S/N diagram of Fig. 14. It should be noted, however, that these data are presented on the basis of the maximum flexural stress in the test members and not necessarily on the basis of the stress at the point of failure, but excellent agreement was obtained for all of the various stiffener details included in the study. All the members obviously had the same flexural resistance but, since the failures occurred at various points along the length

of the span, they did not necessarily have the same fatigue flexural strength at the points of failure.

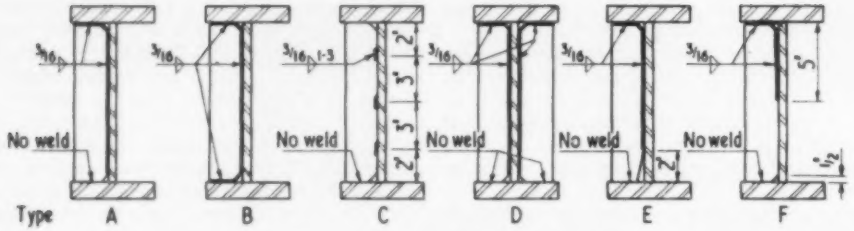
Several factors must be considered in evaluating the data from the stiffener tests. The most important is that most of the fractures occurred at stiffeners that were not in the region of pure moment. In fact, most fractures occurred at stiffeners where the flexural stresses were considerably lower than the maximum flexural stresses.

An S/N curve is also presented in which the maximum bending stress on the extreme fibre at the location of failure is used as the ordinate. These data are shown in Fig. 15. It is apparent from this plot that the maximum bending stress at the fracture section does not provide a consistent relationship in these tests either. A much better correlation is obtained when the data are analysed on the basis of the maximum principal tensile stress (including the effect of shear) at the point of failure. Data plotted on this latter basis are presented in Fig. 16. Although there is still scatter in the test results, there is a well defined scatter band for the entire range of lives and stresses varying from about 13,000 to 35,000 lb/sq.in. ($5\frac{3}{4}$–$15\frac{1}{2}$ tons/sq.in.). It appears that the shear and flexural stress in the web of the welded members and the resulting principal tensile stresses, along with the stress concentrations at the stiffener welds, produced the most critical fatigue condition in the weldments with various stiffeners.

A further indication of the fatigue behaviour of the members with stiffeners can be obtained from an examination of the various fractures. As might be expected, a variety of failures was obtained. With the types *A*, *D*, and *E* stiffeners, those members having continuous welds connecting the stiffeners to the web, the fatigue fractures initiated at the bottom of the fillet weld connecting the stiffener to the web and then proceeded diagonally upward into the web. In addition, it was found that the fractures were likely to initiate at any section between the reaction and the load points near the centre of the member. An illustration

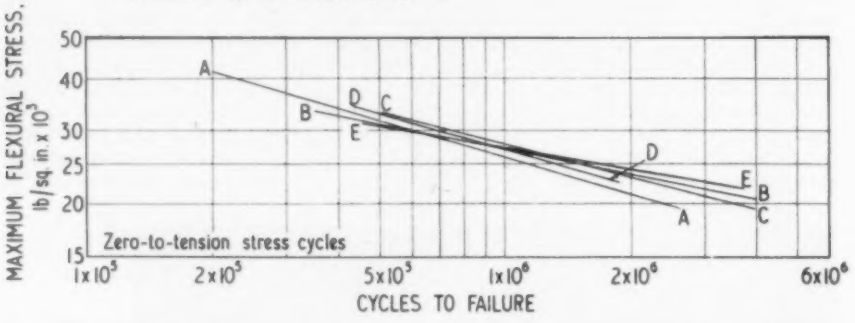


12—*Fatigue cracks in splices at sections under combined moment and shear:* (a) *Failure in web at cope hole;* (b) *secondary failure at cope hole in compression area*

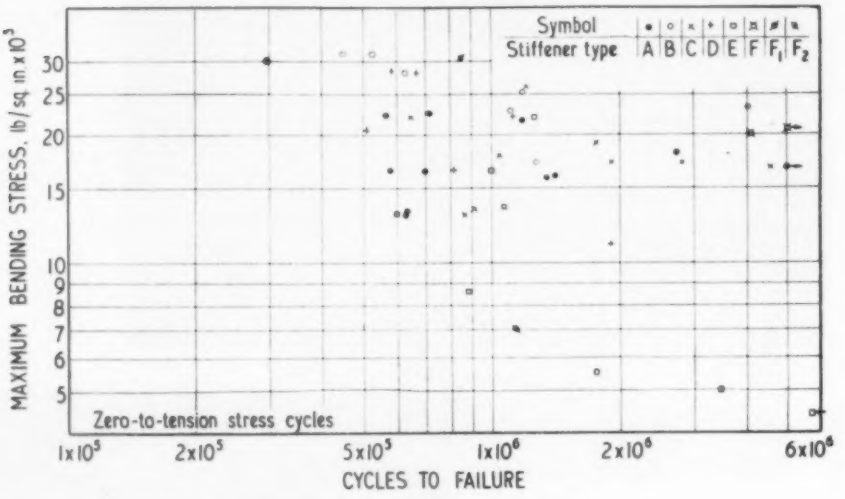


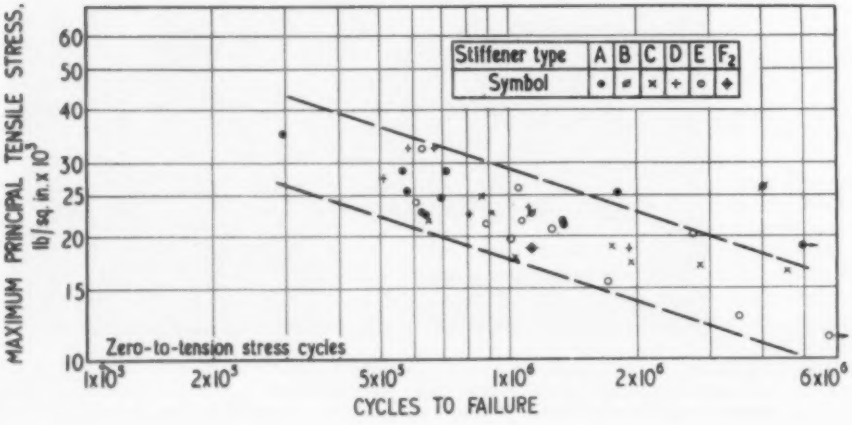
13—*Details of various types of stiffener*

14—*S/N diagram for stiffened beams based on maximum flexural stress*



15—*S/N diagram for maximum bending stress at failure section*





16—*S/N diagram for maximum principal tensile stress at failure section*

17—*Web crack at type* A *stiffener (continuous web weld)*

18—*Web crack at type* C *stiffener (intermittent web weld)*

19—*Web crack at type* F *stiffener (continuous web weld-stiffener cut short)*

of such a failure is presented in Fig. 17. In this instance the crack propagated in the web at an angle of approx. 50°. This angle was somewhat different in other members, depending upon the location of the stiffener at which the failure initiated.

For the members with type *C* stiffeners, the fatigue cracks were found to initiate at either the top or bottom end of the intermittent fillet welds connecting the stiffeners to the web, and then to propagate diagonally upward into the web of the beam. An indication of one such failure may be seen in Fig. 18.

The members with type *B* stiffeners generally failed at the toe of the fillet connecting the stiffener to the flange of the beam. But, in this instance, the failures generally initiated in the region near the maximum moment. After the flange had failed the crack would then propagate up into the web either vertically or at an angle, depending upon the location of the crack along the span length.

One of the last types of stiffener studied, a modification of type *F*, was one which was cut short of the bottom flange. An indication of the fatigue crack for

Table IV

Fatigue data for diagonal tension failures at stiffeners

(Figs. 17, 18 and 19)

Specimen	*Max. Stress at* Extreme Fibre*		*Cycles for Failure*	*Distance of Fracture from Reaction,*	*Principal Stresses at Fracture,† lb/sq.in.*				
	Centre Line	*Primary Fracture*			*Max. Tension*	*Max. Shear*	*Computed*	*Measured*	*Ratio*
	lb/sq.in.			*in.*			*deg.*	*deg.*	
AA-37(O)R + SA	29·4	16·6	574·9	25½	25·7	18·5	56·6	50	1·13
AA-45(O)R + SC	24·9	11·1	1752·7	20	18·9	14·2	54·5	46	1·18
AA-61(O)R + SF	31·0	7·0	1137·8	10	18·7	16·9	47·9	40	1·20

* Stresses based on measurements of section after failure had occurred.
† Stresses computed at the point of initiation of the fatigue crack.

such a member is shown in Fig. 19. In this instance the crack again initiated at the end of the fillet weld attaching the stiffener to the web and propagated at an angle in the web.

In view of the rather unusual nature of the failures for members with stiffeners, it should be pointed out that the webs of these test beams were relatively thin, $\frac{3}{16}$-in. plate material. However, the shearing stress in the webs of the members was not unusually high compared with the maximum flexural stress in the members. This is readily evident in the data of Table IV wherein the stresses for the members shown in Figs. 17, 18, and 19 are summarized.

In spite of the variation in location of the fatigue failures of the members with stiffeners, the best correlation and evaluation of the data were obtained on the basis of the maximum principal tensile stress at the point of fracture initiation. A reasonably consistent correlation was also obtained between the computed angle of the principal stresses and the measured angle of the fracture; however, the computed angle was generally found to be about 20% greater than the measured angle, thereby suggesting that some additional component of stress may be affecting the initiation and propagation of the fatigue cracks.

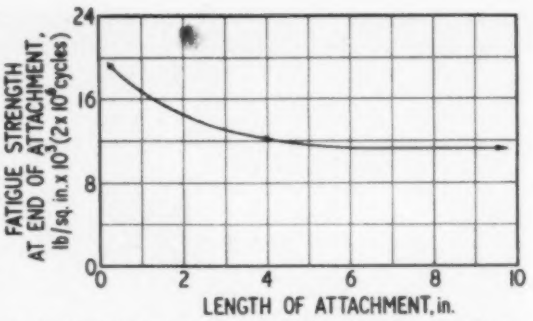
When the stiffeners were welded to the tension flange, the point of failure was at the toe of the fillet weld that joined the stiffener to the flange. However, if the stiffeners were welded to the web but not to the tension flange, the most likely point of initiation was at the end of the fillet weld joining the stiffeners to the web. Nevertheless, the fatigue resistance of these two types of member was not greatly different. As a result, no clear cut picture is available as to whether the transverse fillet weld attaching a stiffener to the tension flange is a serious detriment.

There is, however, an indication in the stiffener tests that the shear in the web of a beam needs to be considered in the development of design criterion for members subjected to repeated loads; if not as an individual factor, the effect of shear upon the principal tensile stress in the web of a member should be considered. The tests produced fatigue failures in members with a maximum principal tensile stress as low as 12,900 lb/sq.in. ($5\frac{3}{4}$ tons/sq.in.). This is certainly of such a magnitude that a careful examination of current design specifications should be made to insure safe and adequate designs.

Straps or Attachments

Since an attachment to the tension flange of a beam changes the geometry of the member, it can be expected that it will change also its fatigue behaviour. However, many different types of attachments may be used with welded beams or girders and can be expected to have different effects upon the fatigue strength of the members.

The reduction in the fatigue strength of tension flanges to which attachments are welded appears to be a function of the attachment length, the way they are attached, and the total forces that are developed in them. The short length over which a stiffener is attached does not produce as great a reduction in fatigue strength as does a longer plate or a partial-length cover plate. Thus, the tensile stress pickup

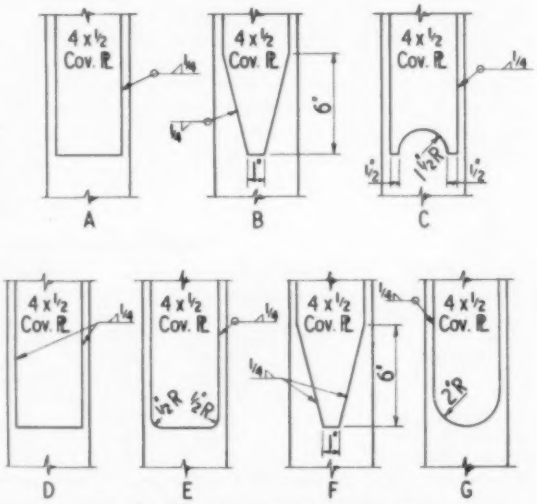


20—*Variation in fatigue strength with length of flange attachment*

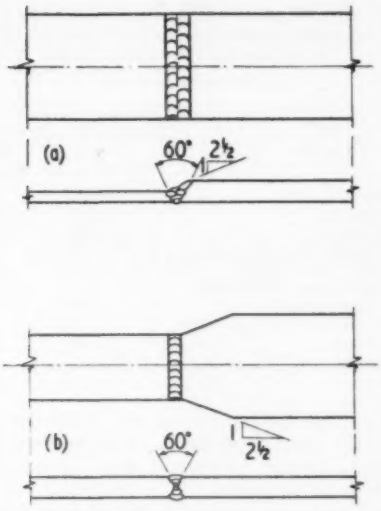
which takes place in an attachment determines, at least to some extent, the reduction that is produced in the fatigue resistance of the member. However, some optimum length can be expected to produce the full reduction.

Although a reduction in fatigue strength will result from the welding of a stiffener to the tension flange of a beam, it is not as great as that obtained with the attachment of a partial-length cover plate; the stiffener is attached to the inner surface of the flange, a point at which the stress is reduced, and over only a very short length of the span. Similarly, a short flat-plate attachment to the outer surface of the tension flange produces a reduction somewhat greater than that caused by the welding of a bar or stiffener to the flange. Pursuing this evaluation of attachments somewhat further it can be concluded that the partial-length cover plate will give the greatest reduction in fatigue strength of the three types of attachment mentioned.

By relating the fatigue strength or reduction in fatigue strength to the length of the various attachments, a relationship of the type shown in Fig. 20 is obtained. This suggests that a cover plate of approx. 12 in. long or more will produce the maximum reduction or the minimum strength for the member, and that this minimum length would be required to develop fully the



21—*Details of various types of cover plate*



22—*Weld details for flanges of beams with flange-plate transitions:* (a) *Transition in flange thickness* (*type* K); (b) *transition in flange width* (*type* J)

participation stresses in the cover plate. Measurements of the strain in a cover plate[7] on a similar member also suggest that only a 12-in. long plate with continuous welds is necessary for the development of the full stress in the plate. This is admittedly a very crude analysis of the behaviour but it does give an explanation that appears to justify the data.

Cover Plates

As already noted, the limiting case of the attachment effect is the partial-length cover plate. This detail, although often used to provide a variation in flexural resistance in a member, can be expected to provide a reduced fatigue resistance at its termination. However, the shape of the cover plate and the manner in which it is attached will also have an effect on fatigue strength.

In recent years, studies have been made on a variety of cover or flange-plate details.[2,3,8] The details of the cover plates used in one of the most recent investigations are shown in Fig. 21. Another means of obtaining a variation in the flexural capacity of the section is by varying the width or thickness of the flange plates in the manner shown in Fig. 22. These latter details, because of the more uniform transfer of stress they provide, will give the greatest fatigue resistance for the various members of varying section.

Only a limited number of tests has been conducted on each of the cover-plate details. Nevertheless, they provide data for S/N curves for a loading cycle of zero-to-tension. A summary of these data is presented in Table V, which shows that there is considerable variation in the fatigue resistance of the members with various cover-plate or flange-plate details.

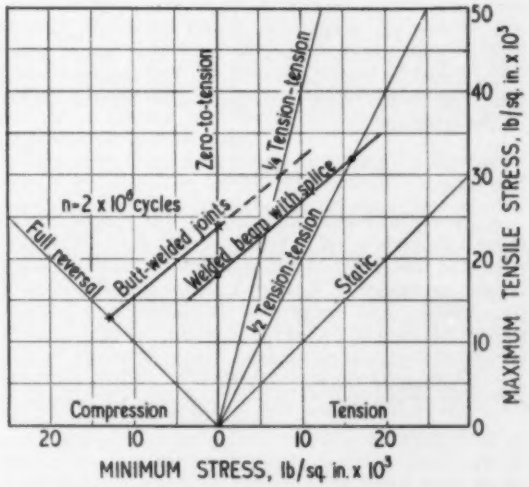
An examination of the results of the tests on members with cover plates indicates also that any means used to make the change in cross-section of a beam more gradual was effective in increasing the life of the beam under repeated loads. Under high loads the beams with the best performance were those with type *F* cover plates; this type of end-detail combined the beneficial effects of a tapered end and no transverse weld. Under the lower loads, beams with type *C* end detail gave the best results. The lowest fatigue resistance under either high or low loads was obtained with the type *A* cover plate. The omission of the transverse welds at the ends of the cover plate thus increases the fatigue resistance of the members. However, one of the most effective means of increasing the fatigue resistance of a welded beam of variable section was to vary the flange in width or thickness. In this instance values of fatigue resistance almost as great

Table V

Summary of fatigue strengths of beams with various changes in flange area

(Zero-to-tension cycle)

Description	*Fatigue Strength, lb/sq.in.* 10^5 *cycles*	2×10^6 *cycles*
Partial-length cover plates, square ends with continuous weld all round (type *A*)	26,500	11,300
Partial-length cover plates, tapered ends with continuous weld all round (type *B*)	33,000	11,500
Partial-length cover plates, concave profile with continuous weld all round (type *C*)	30,700	14,500
Partial-length cover plates, square ends with continuous weld along edges only (type *D*)	34,700	12,500
Partial-length cover plates, tapered ends with continuous weld along edges only (type *F*)	36,500	13,700
Partial-length cover plates, convex profile with continuous weld all round (type *G*)	29,000	11,700
Butt-welded flange transition, tapered in width (type *J*)	34,900	19,500
Butt-welded flange transition, tapered in thickness (type *K*)	34,600	18,500



23—*Modified Goodman diagram for welded beam with butt-welded splice*

as that of the spliced beams was obtained, again demonstrating the advantage of reducing to a minimum the stress concentrations when fatigue is of concern.

Laboratory Data and Service Conditions

It is obvious that the laboratory data in a single S/N diagram, such as that presented in Fig. 16, does not provide sufficient information to relate the behaviour of the members to all field or service conditions. To do so would require a series of such diagrams, each of which represented a different type of loading cycle. Such data, if of sufficient range and variation, would then permit the presentation of a more extensive fatigue relationship in the form of a Goodman or modified Goodman diagram, which could more easily be related to various service conditions. However, the preparation of such relationships generally requires a great deal of test data.

With only limited data available, approximations of the form presented in Fig. 23 may be used to show the relationships between the maximum and minimum stresses for failure in a particular member and at a given number of cycles. Interpolations or extrapolations of such data may than be related approximately to actual service conditions.

For example, a typical welded railway girder bridge might be expected to receive maximum stresses ranging from about 4,500 to 18,000 lb/sq.in. (2–8 tons/sq.in.). For a splice subjected to a loading of $\frac{1}{4}$ tension-to-full tension, the data of Fig. 23 would suggest that a load cycle of about 6,000 to 24,000 lb/sq.in. ($2\frac{1}{2}$–$10\frac{3}{4}$ tons/sq.in.) would be necessary to produce failure in 2×10^6 cycles. Thus for a welded splice in the most stressed region, only a small factor of safety would exist against failure in the members at 2×10^6 cycles. However, with the splice placed in a less highly stressed region, as is usually the case, a greater factor of safety against failure would exist for a sound welded splice.

Welded splices, because of the manner in which they are located in low-stress regions of welded-beam structures, will generally not provide a point of weakness if they are properly prepared. However, partial-length cover plates, owing to their low fatigue resistance, must be used cautiously or they may provide a condition that is likely to initiate failure during the life of the structure.

Conclusions

A variety of welded details and their effect on the fatigue behaviour of a welded beam have been discussed in this paper. Although these details all reduce fatigue resistance of a welded beam, sound design of the members and the proper positioning and fabrication of the details, based on an understanding of their behaviour, make it possible to design structures which are of adequate fatigue capacity. As shown in Table VI, most of the details can be expected to produce a relatively high fatigue resistance; only the partial-length cover plate produces a major reduction in the fatigue strength and, as a result, must be terminated at points of reduced stress.

Table VI

Summary of approximate fatigue strengths of welded beams with various details

(Zero-to-tension cycle)

Member or Section	*Fatigue Strength lb/sq.in. 2×10^6 cycles*	*Ratio to Plain Plate*
Plain plate	31,700	1·0
Rolled **I** beam	31,200	0·98
Welded **I** beam	26,000	0·82
Welded beam with stiffeners	23,000	0·73
Welded beam with splice	20,000	0·63
Welded beam with butt-welded flange transitions	19,000	0·60
Welded beam with partial-length cover plate	12,500	0·39

On the basis of the data discussed herein it is suggested also that, in fatigue, consideration should be given to the effect of shear on the principal tensile stresses at the stiffener welds on the webs of stiffened welded beams, particularly when thin web members are used. Unless this is done, diagonal tension fatigue failures may be likely.

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SYMPOSIUM ON FATIGUE OF WELDED STRUCTURES

Effect of Mean Stress on Fatigue Properties of Aluminium Alloy Butt-Welded Joints

By K. W. Gunn and R. McLester

Axial load fatigue tests on butt welded joints made in sheet and plate conforming to specifications BS.1470 NS4½H, BS.1477 NP5/6M, and BS.1477 HP30WP are reported. The tests were done at mean stresses of zero, 1½, 3, 5 and 7 tons/sq.in. to provide comprehensive information on the fatigue of butt welds on which design data might eventually be based. At the present time, no adequate design rules exist for aluminium alloy welds.

The work shows that the three alloys have very similar fatigue strengths when butt welded. With increasing mean stress, above about 4 tons/sq.in. for any particular endurance, there is very little decrease in the range of stress for any of the alloys.

The scatter of the results is discussed.

At the present time there are no adequate design rules to cover the fatigue of welded structures made in aluminium alloys. A specification[1] for structures of a moderate strength aluminium alloy, issued by the American Society of Civil Engineers, states that a well designed structure will withstand 10,000 repetitions of the live load without fatigue failure. For longer lives the designer has to refer to technical literature. The reason for this lack of guidance in designing against fatigue is that it is impossible to forecast the fatigue strength of a welded joint from any data other than results of fatigue tests on a similar welded joint. There has been little published information available on British alloys or, indeed, on any alloys similar to those manufactured in Britain. It was therefore decided to test three of the more common aluminium alloys used in structures, namely, BS.1470 NS4½H, BS.1477 NP5/6M and BS.1477 HP30WP.* An inherently good design of welded joint, *i.e.*, a butt weld, was chosen for this, the first part of the work, because it was expected that butt welds would have as high strengths as could be obtained from welded joints.

Since the object of the work was to obtain results that could be used as the basis for working stress diagrams for good commercial welds, the butt welds tested were not made to inordinately high standards, nor were the weld beads removed. For this reason also, the tests were carried out at several mean stresses, so as to cover most combinations of live and dead loading that are likely to be encountered in a structure.

Paper to be presented at a Symposium on Fatigue of Welded Structures, to be held at Cambridge University on 29th March to 1st April, 1960. 540

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The authors are at Aluminium Laboratories Ltd., Banbury.

* The materials tested were manufactured by Northern Aluminium Company Ltd., and are designated by them Noral M57S½H, Noral B54SM and Noral B51SWP.

Materials Used in Tests

The three aluminium alloys selected for the work are corrosion-resistant alloys commonly used in structural engineering. Types NS4 and NP5/6 are Al–Mg alloys whose properties are controlled by work hardening in the manufacturing process and they are not normally heat treated.

Alloy NS4 is used for structures exposed to particularly severe corrosive atmospheres and to moderately elevated temperatures; for example, in heat exchangers. The suffix ½H shows that the sheet has been cold rolled as the final manufacturing operation and to a temper at which it achieves a desirable combination of properties.

Type NP5/6 is the recommended alloy for structures built with plate for service at atmospheric and low temperatures; for example, in shipbuilding and in liquid methane plant. The suffix M denotes that the material has not received any further treatment after hot rolling.

For the heat-treatable Al–Mg–Si alloy HP30 the suffix WP shows that the material has been solution heat treated and artificially aged to develop its full mechanical properties. It is recommended for general structural applications, such as road and rail transport vehicles, bridges, and cranes.

All materials were in the form of ¼ in. thick sheet or plate. The average chemical analyses and tensile properties are given in Table I. The joints were manually welded by an inert-gas metal-arc (Mig) process.

Table I

Chemical analyses and tensile properties of parent materials

Alloy	*Analysis, %*	*Cu*	*Mg*	*Si*	*Fe*	*Mn*	*0·1% proof stress tons/sq.in.*	*U.T.S., tons/sq.in.*	*Elong. on 2 in, %*
NS4½H	Sheet	0·027	2·11	0·11	0·31	0·11	13·0	15·9	12½
	Specification	0·10 max.	1·8–2·7	0·6 max.	0·7 max.	0·5 max.	12·0 min.	15·0 min.	5 min.
NP5/6M	Plate	0·025	4·36	0·12	0·28	0·60	13·2	19·75	19½
	Specification	0·10 max.	3·5–5·5	0·6 max.	0·7 max.	1·0 max.	8·0 min.	17·0 min.	12 min.
HP30WP	Plate	0·029	0·65	1·04	0·28	0·55	18·6	21·25	14
	Specification	0·10 max.	0·4–1·5	0·60–1·3	0·6 max.	0·40–1·0	15·0 min.	19·0 min.	8 min.

Each value is the average of 4 determinations.

They were welded on one side into a temporary grooved steel backing bar using NG6 filler wire, back chipped, and then welded on the reverse side to give symmetrical weld profiles. Full welding details are given in Fig. 1, and a typical test piece is shown in Fig. 2. To avoid incorporating the start or finish of a weld in a test piece, plates between 12 and 36 in. wide were welded, the ends of the welds were discarded, and test pieces 3½–4 in. wide were cut from the remaining weld. The edges of the test pieces were machined before testing. Tensile specimens were taken from the joints—the average tensile properties are given in Table II.

Table II

Tensile properties of butt-welded test-pieces in ¼ in. thick material

Alloy	*0·1% proof stress, tons/sq.in.*	*U.T.S., tons/sq.in.*	*Elong. on 2 in., %*
NS4½H	6·4	13·1	14
NP5/6M	8·8	18·7	17
HP30WP	6·9	11·8	8½

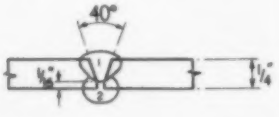
Each value is the average of at least 10 determinations.

Test Procedure and Results

The fatigue tests were carried out in Amsler Vibrophore High-Frequency 10-ton axial load machines. The frequency of testing was about 12,000 cycles/min. Tests were run until the joint failed or the life reached 10^7 cycles. The joint was considered to have failed when a crack could be seen with the naked eye. Test pieces which endured 10^8 cycles without failure were retested at a higher stress.

The tests were carried out with tensile mean stresses of zero, 1½, 3, 5, and 7 tons/sq.in. All stresses were calculated on the area of the plate adjacent to the weld. The results are shown plotted as S/N curves in Figs. 3–5. Average curves have been drawn through each set of points, and from these curves plots of R (semi-range of stress) against M (mean stress) at endurances of 10^5, 5×10^5, 10^6, 10^7, and 10^8 cycles have been drawn (Fig. 6), because it is from this type of diagram that working stress diagrams can most easily be derived.

A few tests, using a compressive mean stress of 2 tons/sq.in., were run with the alloy NP5/6M. These results are shown in Fig. 7. All the joints tested at zero mean stress were X-rayed and the radiographs were



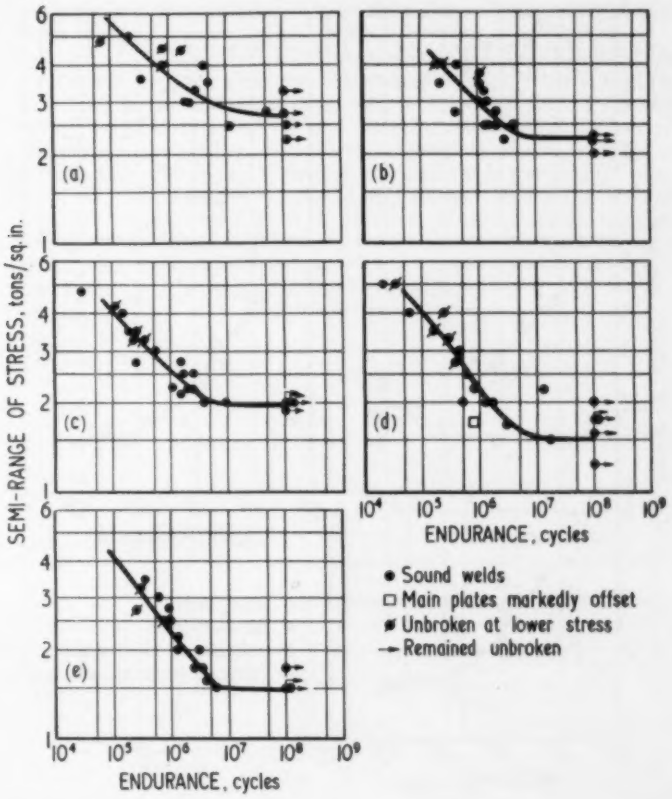
Joint spacing—$\frac{1}{16}$ in. Current—240–245 amp
Arc voltage—26 ± ½ V Arc travel speed—12 in./min (approx.)
Filler wire—NG 6
Temporary backing bar employed

1—*Edge preparation and welding details for butt-welded test pieces*

(a)

(b)

2—*NP5/6M welded joint used in tests. Note fatigue crack at lower edge of weld bead in* (a) ×½



3—S/N curves for butt welds in ¼ in. thick NS4½H sheet welded both sides with NG 6 filler wire (Mig process): (a) Zero mean stress; (b) mean stress 1½ tons/sq.in.; (c) mean stress 3 tons/sq.in.; (d) mean stress 5 tons/sq.in.; (e) mean stress 7 tons/sq.in.

examined for signs of porosity and lack of penetration. Slight porosity was found in some of the joints but it bore no relation to the results of the fatigue tests.

R/M Diagrams

Derivation

A considerable amount of scatter was found in the test results, and so after they had been tested several joints were examined in an attempt to relate their physical characteristics to their position in the scatter band. Most of the fatigue cracks started at the junction of the plate and the weld, as in the example shown in Fig. 2. One of the joints giving a low result was found to contain excessive porosity. This joint failed at the centre of the weld, and the fact that porosity was the cause of failure was obvious on visual examination of the fractured surface. Others of the low results were found to be due to badly formed weld beads acting as stress raisers. The results from these faulty joints are distinctively marked on the S/N diagrams and have been ignored in drawing the mean curves but included when estimating the scatter.

Shape of R/M curves

The R/M curves for the three alloys are of the normal form for test pieces containing a stress raiser; the characteristic levelling-off at high mean stresses is exhibited[2, 3] (see Appendix). However, it has not proved possible to relate the commencement of this level portion to the yield point of the material, using the method proposed by Gunn[2]. This is to be expected, since the properties of the material, the stress concentration fact, and the residual stresses induced by welding at the origin of fracture are not accurately known.

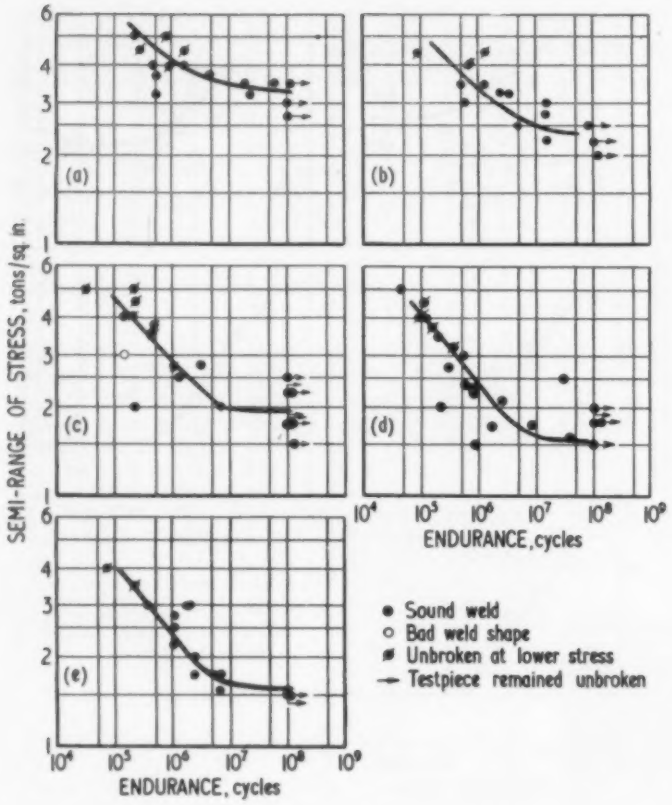
Comparison of alloys

The three alloys can be seen to have very similar fatigue strengths in the welded state, particularly at long endurances and high mean stresses. The scatter in the NS4½H test results is markedly less than that for the other two alloys.

It could be inferred from the later discussion on scatter that this is characteristic of the alloy because it has a low yield point when welded. It has, however, been assumed when considering the results that it happened purely by chance.

Scatter and R/M curves

It is almost impossible to establish by fatigue testing a minimum curve of practical value below which no test result will ever fall, so it is necessary to relate the lower limit of scatter bands to a probability of survival. One way of doing this is to plot, on the R/M diagrams, curves that are derived from a set of S/N survival curves. A survival curve is a line drawn so that a known percentage of the lives fall below the curve for



4—S/N curves for butt welds in ½ in. thick NP5/6M plate welded both sides with NG 6 filler wire (Mig process): (a) Zero mean stress; (b) mean stress 1½ tons/sq.in.; (c) mean stress 3 tons/ sq.in.; (d) mean stress 5 tons/sq.in.

every test stress. For example, with a 95% survival curve 5% of the results fall below and 95% lie above the curve. The relatively small number of test results available precluded the use of the conventional method of obtaining survival curves, which consists of determining the distribution of lives at several stress levels. The following method, which assumes that the S/N curves for all probabilities of survival are displacements of the average curves parallel to the stress axis, was therefore used; it is an approximate method but probably it errs on the safe side.

On each S/N diagram the vertical deviation (*i.e.*, the 'stress distance') between each plotted point and the average S/N curve was measured. All distances were then grouped and tabulated in order of magnitude irrespective of alloy, so that the level above which 95% of the results lay could be determined. The '95% survival R/M curves' were then drawn by lowering the average R/M curves by the stress distance corresponding to this level. The curves for 10^6 cycles are illustrated in Fig. 8. The S/N curves were plotted on log-log paper and so the range of stress in the R/M diagrams was also plotted logarithmically (*see* p. 206).

The line that cuts off 5% of the test results from the remainder is based on this particular series of tests. It is an estimate of the line that cuts off 5% of results for all similar welded joints, but since its position is based on only a sample it may in fact cut off between 2½% and 8% of the total population. The values 2½% and 8% are the 95% confidence limits.

Effect of Flaws

The welds that were made for this investigation were not entirely free from flaws, and some of them have affected the fatigue properties. These flaws are discussed in the following section but no attempt has been made to give a complete picture of the effect of flaws in general although, of course, the following observations might have a wider application.

Porosity

The normal slight porosity (generally in the weld bead) that might be found in properly made welds appears to have no effect on fatigue strength because the cracks usually start at the edge of the weld bead. However, it would be expected that excessive porosity, due to faulty welding technique, would lower the fatigue strength.

Offset between plates

Offset between plates is a common fault in butt joints and is extremely difficult to avoid in long runs without considerable clamping. It induces bending stresses under axial loading and thus lowers the fatigue strength. An example of its effect is given in Fig. 9, where the results of axial load and bending tests on NS4½H ⅛ in. thick sheet with considerable misalignment of the plates (up to 0·020 in. displacement of the axes) are compared with the average S/N curve for zero mean stress taken from Fig. 3. It can be

seen that misalignment is of little importance in bending, but has serious consequences under axial load.

Undoubtedly some slight misalignment was present in some of the joints that were tested but no relationship could be observed between the amount of misalignment and the position of the test point in the scatter band. The test result from the joint with the greatest offset is shown in Fig. 3.

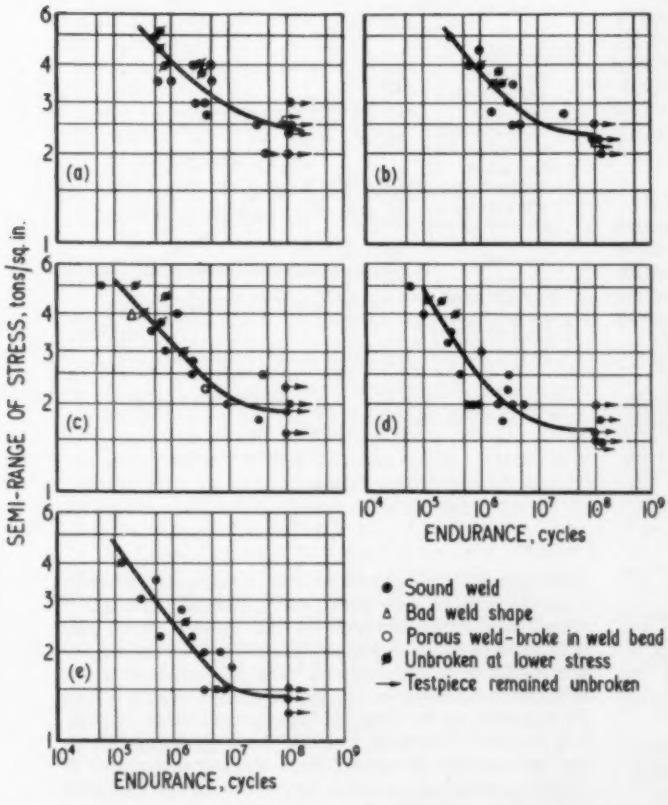
Shape of weld bead

A high stress concentration at the edge of a weld bead, caused by the shape of the intersection of bead and plate, will lower a joint's fatigue strength. Examples of this effect are shown in Fig. 10, where results from joints in NP5/6M and HP30WP[4] with well defined beads, made by welding from one side against a grooved backing bar, are compared with the S/N curves for $r=-1$ and $r=0$* taken from the S/N curve in Fig. 4*a*, and from the R/M diagrams in Fig. 6. Most of the results from joints welded from one side are markedly lower than those from joints welded from two sides.

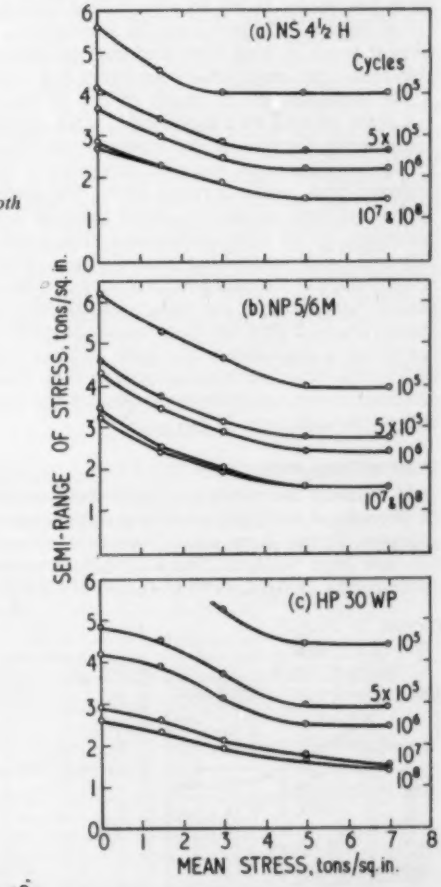
The weld bead when welded from both sides is not always free from a sharp stress raiser. When a weld run is back-chipped before it is welded on the reverse side, it can happen that varying depths of metal are removed at different points, particularly on a long run. When the reverse pass is made it is possible for overheating to occur at the thinner sections with consequent flow of the first run. This flow results in a misshapen weld bead, sometimes almost rectangular in

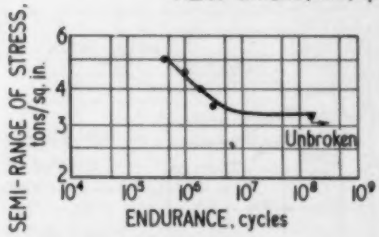
* r is the ratio $\frac{\text{minimum stress in cycle}}{\text{maximum stress in cycle}}$

6—*Average R/M curves for butt-welded joints welded from both sides. NG 6 filler wire (Mig process)*



5—*S/N curves for butt welds in HP30WP plate ¼ in. thick plate welded both sides with NG 6 filler wire (Mig process): (a) Zero mean stress; (b) mean stress 1½ tons/sq.in.; (c) mean stress 3 tons/sq.in.; (d) mean stress 5 tons/sq.in.; (e) mean stress 7 tons/sq.in.*





7—*Effect of compressive mean stress. S/N curve for butt welds in ¼ in. thick NP5/6M plate, welded from both sides with NG 6 filler wire (Mig process)*

cross-section. This occurred with two test pieces, one each in NP5/6M and NS4½H, but without serious effect in either case.

Discussion

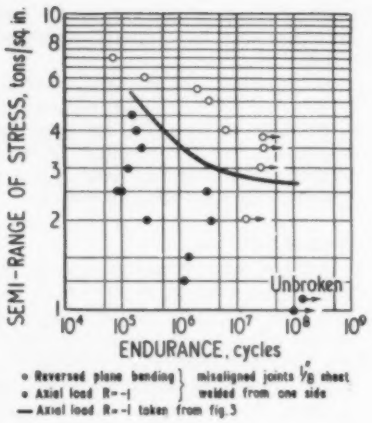
Scatter of results

Part of the considerable scatter of results has been accounted for as being due to flaws, but there remained a number of test pieces giving low values, which, upon being broken open, displayed no obvious defect. These joints failed in the normal manner at the edge of the weld bead, and the fractured surfaces revealed no unusual features. Micro-examination of several of these test pieces failed to account for the low results, and it must be accepted that the scatter band for butt-welded joints is very wide and that otherwise acceptable joints can occasionally give very low results.

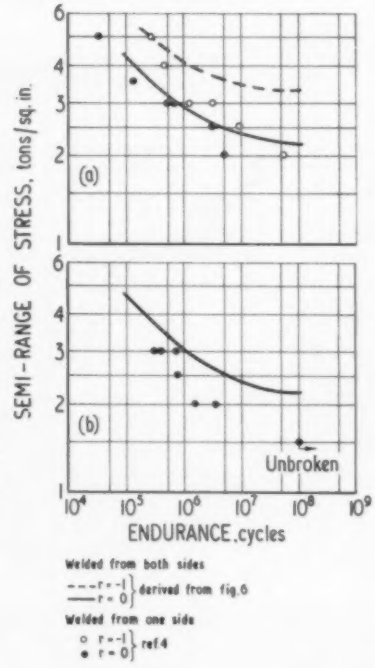
The semi-ranges of stress in the R/M curves of Fig. 8 are plotted on a logarithmic scale. There is some justification for this. If the scatter were due primarily to stress raisers of varying intensity it would be expected that, under a mean stress, the scatter would be less at high ranges of stress than at low ranges. Similarly, at all long endurances, the scatter should decrease with increasing mean stress. Both effects result from the reduction in local mean stress caused by plastic flow induced by high peak stresses round the stress raisers. It follows from these effects that parallel curves on a logarithmic plot give a closer approximation to the expected behaviour than they would on an arithmetic plot, and that the curves on the log-plot are still on the safe side.

Other published work

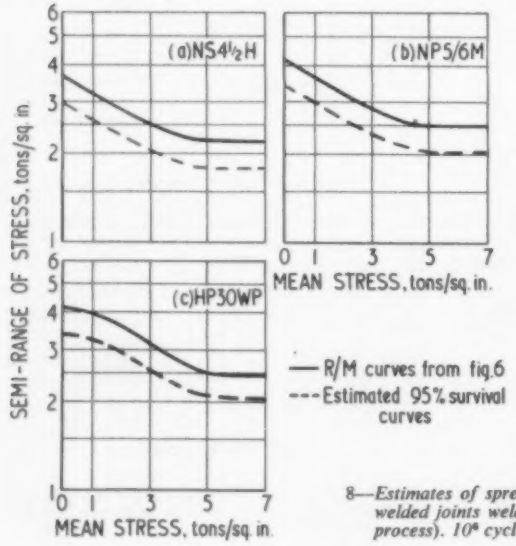
In most of the previously published work that can be compared with the present results only one stress ratio ($r=0$) has been used; the effect of mean stress has not been studied. The equivalent curves for the three alloys NS4½H, NP5/6M, and HP30WP, derived from the R/M diagrams in Fig. 6, are compared in Figs. 11 and 12 with previously published results,[5–7] which are superimposed on the curves. With the exception of those published in 1954 by Hartmann, all the results agree very closely. The inherent variations in different designs of testing machines, which might be expected to be a major factor, have been of little importance when testing joints. The reproducibility in the laboratory of results from welded test-pieces is clearly established; whether or not this can be repeated in service is considered in the next section.


SEMI-RANGE OF STRESS, tons/sq. in.
ENDURANCE, cycles
Unbroken
Reversed plane bending
Axial load R=-1
misaligned joints 1/8" sheet welded from one side
Axial load R=-1 taken from fig. 3

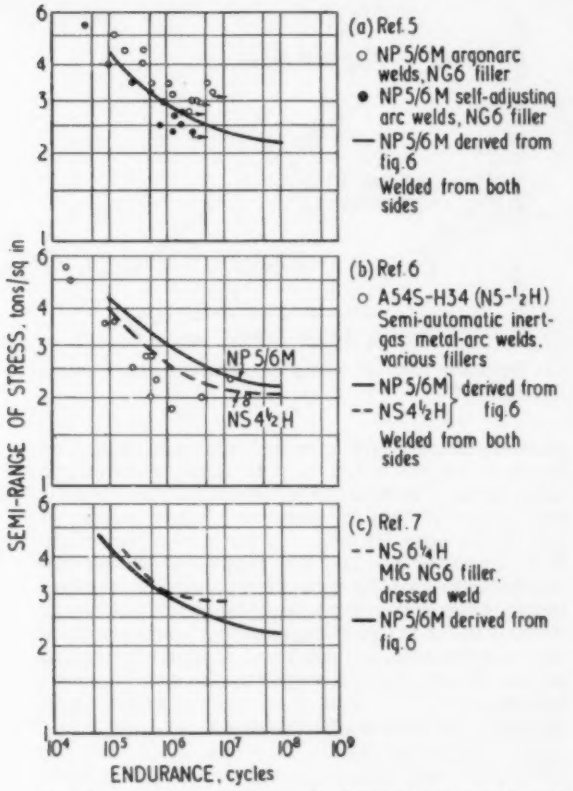
9—*S/N curves for butt welds in NS4½H sheet. Comparison of satisfactory and misaligned joints*



10—*S/N curves for butt welds welded from both sides compared with test results from butt welds welded from one side over a grooved backing bar. NG 6 filler wire (Mig process): (a) NP5/6M plate ¼ in. thick; (b) HP30WP plate ¼ in. thick*



8—*Estimates of spread of test results. R/M diagrams for butt-welded joints welded from both sides. NG 6 filler wire (Mig process). 10^8 cycles*


SEMI-RANGE OF STRESS, tons/sq in
(a) Ref. 5
○ NP 5/6M argonarc welds, NG6 filler
● NP 5/6 M self-adjusting arc welds, NG6 filler
— NP 5/6 M derived from fig. 6
Welded from both sides
(b) Ref. 6
○ A54S-H34 (N5-½H) Semi-automatic inert-gas metal-arc welds, various fillers
NP 5/6M
NS 4½H
— NP 5/6M } derived from
-- NS 4½H } fig. 6
Welded from both sides
(c) Ref. 7
-- NS 6¼ H
MIG NG6 filler, dressed weld
— NP 5/6M derived from fig. 6
ENDURANCE, cycles

11—*Comparison of NS4½H and NP5/6M test results with previously published results* ($r=f_{min}/f_{max}=0$)

Application of results to design of structures

Fairly large test pieces were used so that 'size effect' would not be a major uncertainty in transforming the results to design data. The average curves probably give good indications of the shapes of the S/N and R/M curves, but there are not enough results to give the shapes of the curves for very high probabilities of survival. There were, however, about 80 test pieces made in each alloy, and so unless several hundreds of specimens were to be tested, it would not be possible to obtain appreciably more reliable estimates of the survival curves than those in Fig. 8.

The lower limit will be governed to some extent by the quality of the weld itself; for example, porosity, sharpness of angle of weld bead, and misalignment of the main plates will, with increasing severity, all tend to lower the scatter band. The effects of these three have already been noted (p. 204), but it seems that the effect of a moderate degree of imperfection of the weld is not serious compared with the inherent scatter of the tests on satisfactory joints. But much more work is needed before the importance of defects can be established.

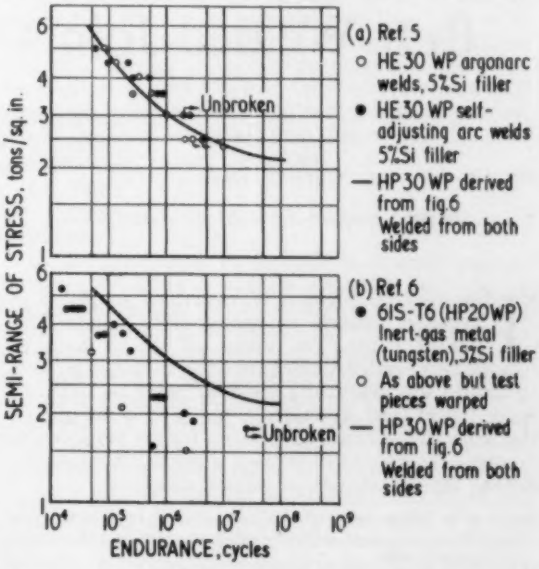
The more or less horizontal portion of the R/M diagrams at the higher mean stress is a most helpful observation, even though the maximum stress (mean stress plus semi-range of stress) is above the normal working stress for much of this region. It means that built-in assembly stresses can be virtually ignored when establishing working stresses. On the other hand, the few tests with compressive mean stresses confirm that residual compressive stresses (provided that they are not dissipated by plastic flow) will have a beneficial effect on the fatigue strength.

The ASCE[1] recommend a design stress of 8,000 lb/sq.in. for welds in 6061-T6 aluminium alloy, a structural alloy similar to HP30WP. This stress is stated to give a life of at least 10,000 cycles for all designs of joint. The present results confirm that with this maximum stress butt welds have longer lives than 10,000 cycles at zero mean stress and have even greater lives under high-tensile mean stresses. However, other designs are known to be weaker in fatigue than butt welds, but it is not possible to state, from the results of these rests on butt welds, how much weaker they would be. Consequently, it is not yet possible to state that all designs will be safe at the ASCE recommended stress.

It would, of course, be most satisfactory if the specifications were to cater for many designs of joint, but this is impracticable and so it is proposed that in any future work one or perhaps two designs of joint that will give low results should be tested fairly thoroughly. Designs that might very well be incorporated in normal structures should be chosen. The highest possible and lowest likely fatigue properties from practicable joints will then be available to designers, who can then check whether or not there is likely to be a fatigue failure in their structures and, if it is likely, to what extent it can be prevented by refining the design.

Conclusions

All three alloys, NS4½H, NP5/6M, and HP30WP, *i.e.*, both non-heat-treated alloys and the one heat-treated, have very similar fatigue strengths. With increasing mean stress the range of stress decreases


SEMI-RANGE OF STRESS, tons/sq. in.
(a) Ref. 5
○ HE 30 WP argonarc welds, 5% Si filler
● HE 30 WP self-adjusting arc welds 5% Si filler
— HP 30 WP derived from fig. 6
Welded from both sides
Unbroken
(b) Ref. 6
● 61S-T6 (HP20WP) Inert-gas metal (tungsten), 5% Si filler
○ As above but test pieces warped
— HP 30 WP derived from fig. 6
Welded from both sides
Unbroken
ENDURANCE, cycles

12—*Comparison of HP30WP test results with previously published results* ($r=f_{min}/f_{max}=0$)

until about 4 tons/sq.in. mean stress, above which the range remains almost constant.

In general, minor imperfections in the weld do not reduce the fatigue strength below the lower limit of the scatter band obtained with sound joints, but major imperfections could do so. The dividing line between major and minor imperfections is difficult to define without further tests, but it is suggested that a weld of good appearance made by correct techniques, with only a small amount of porosity and offset, will have good fatigue properties.

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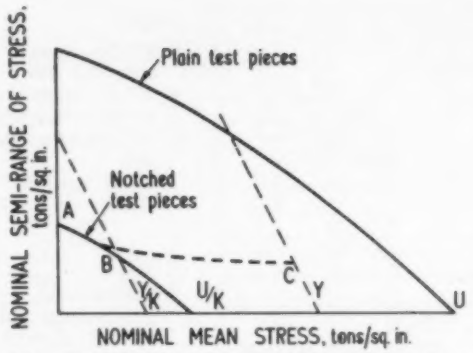
APPENDIX

The shape of the R/M curve for notched test pieces can be derived from the R/M curve for plain test pieces by applying the appropriate strength reduction factor K, for the notch, to both range of stress and mean stress. The R/M curve for notched test pieces is thus similar to that for plain ones but is reduced in scale in the ratio $1/K$ (Fig. 13). In the first part of the R/M curve for notched test pieces the stresses are purely elastic, and the curve AB is parallel to the curve for plain test pieces, but when local yielding occurs at the base of the notch, increasing mean stress produces only a slight increase in the local maximum stress and so has little effect on the range of stress until very high stresses are reached. This gives a curve ABC, in which the portion BC is almost horizontal.



13—*Theoretical effect of yielding on fatigue strength of notched test pieces*[2]

Increasing the Fatigue Strength of Butt-Welded Joints

By Dr. rer. nat. Werner Gilde

The fatigue limit under cyclic stress of butt-welded joints depends on the surface of the weld bead and on the surrounding medium. By covering the weld bead with plastic material the surface is given a smoother finish; also a medium is provided capable of increasing the fatigue strength. This method enables the dynamic strengths of butt welds to be improved by 75%.

THE FATIGUE strength of structures subjected to dynamic loading is considerably reduced by welded joints. The values of the strength of dynamically loaded welded joints in high-tensile steel structures are practically only the same as those of welded mild steel structures (Fig. 1). It is the aim of this work to determine the reasons for the reduction in fatigue strength and to provide a method for increasing the fatigue strength of welded structures.

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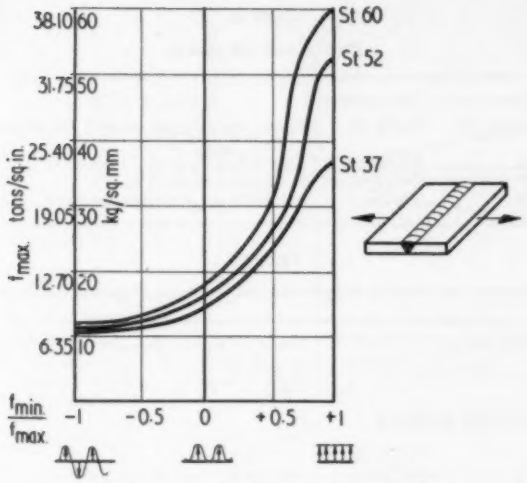
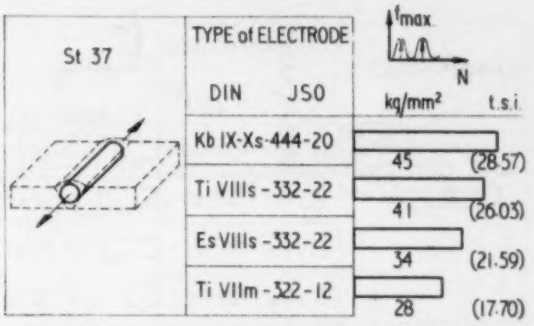
Reasons for Changes in the Fatigue Strength of Welded Joints

Composition of weld metal

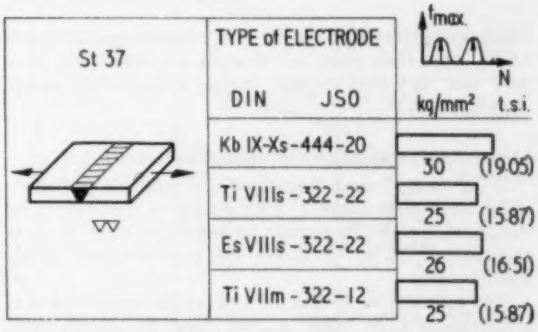
An examination of either the weld metal itself or the welded joint with machined surfaces will reveal the influence of the weld metal on fatigue strength. Figures 2 and 3 show the values according to Becker and Rieger.[1] A comparison of these diagrams indicates that the welded joint has a lower fatigue strength under pulsating stresses than that of an all-weld-metal specimen, although the same types of electrode are involved. In both cases the superiority of low-hydrogen type (basic) electrodes has been proved.



1—*Fatigue strength values for mild steel welds*



2—*Influence of weld metal on fatigue strength of all-weld-metal specimens*



3—*Influence of weld metal on fatigue strength of machined butt welds*

Influence of the surface

It is well known that notches have a marked influence on fatigue strength, and that such stress raisers are able to affect welded joints to a considerable extent. A welded joint prepared with an electrode producing a rough surface will have a lower fatigue strength than a joint made with a smooth-surfaced deposit. It may be seen in Fig. 4 that lower fatigue strength values are obtained for unmachined butt-joints with low-hydrogen electrodes than with rutile electrodes or electrodes with a covering of the iron-oxide type giving inflated slag (class 4 electrodes), although Figs. 2 and 3 indicate that low-hydrogen weld metal has a higher fatigue resistance under pulsating stresses.

Influence of surrounding medium on fatigue strength

The results of the research work of Benedicks[2] reveal that the fatigue strength depends upon the surrounding media (Table I).

Table I

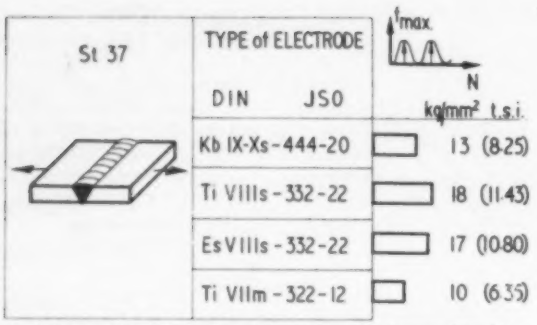
Influence of the medium on fatigue strength[2]

Material	*Medium*	*Change of Fatigue Strength, %*
13% Cr steel	H_2O	−40
Glass	H_2O	−38
Glass	Ethyl alcohol	−23
Glass	Petroleum	+16
0·75% C steel	H_2O	−22
0·75% C steel	Ethyl alcohol	−13
0·75% C steel	10% NaOH	+15
0·75% C steel	Benzine	+18

As may be seen, some fluids are capable of decreasing the fatigue strength and others of increasing it. It is not intended to investigate here to what extent changes in surface tension may depend upon the influence of various fluids. Further, it is well known that gases also may have a certain effect. Thompson[3] encountered an increase in the fatigue strength of steel in a nitrogen atmosphere as related to values in air. The results of studies of Gough and Sopwith[4] showed an increase in the fatigue strengths of steel by 5%, copper by 13%, and brass by 26%, when exposed to a vacuum of 10^{-3} mm of mercury. Hence, it can be concluded that even adhesive coatings of solid materials may be capable of changing the fatigue strength. Matting[5] states that the fatigue strength of a steel specimen with a hole in the centre was increased when the hole was welded up with a material having a modulus of elasticity less than that of steel. He used austenitic and bronze electrodes.

Experiments to Increase Fatigue Strength

As already shown, an increase in strength may be achieved by using a suitable electrode material, by surface machining, and by changing the surrounding media. Electrode materials are fixed to a great extent by standard rules. Therefore an attempt must be made to attain the best fatigue strength values by changing the surface and the surrounding media. Since there is normally no opportunity of surrounding welded joints with fluids or gases, it was an obvious step to substitute these with solid materials able to produce a uniform and, if possible, an airtight covering of the weld surface. Such materials are simultaneously expected to be capable of increasing the fatigue strength. A further requirement is that these materials should fill up notches and irregularities in welds so as to avoid any expensive machining of the weld bead.



4—Influence of weld metal on fatigue strength of unmachined butt welds

Ultimately, the degree of elasticity and fatigue strength of the materials must be of such an order that they will not fail before the metal component under dynamic load.

Choice of surface coating for increasing fatigue strength

It has been found that plastics, if applied to metals either in a pure state or with additives, proved to be the most suitable materials to meet the requirements mentioned. Table II shows the composition of the plastic used.

The application of the plastics to the specimens was accomplished in such a way that all weld bead irregularities were filled up. The plastic material was spread out to cover the heat-affected zone also, and a smooth and uniform transition from the surface of the parent metal to that of the plastic material was obtained. Rust, scale, and grease had been removed mechanically from the metal before applying the plastic coating. Depending on their composition, the plastic materials hardened within 24 hr.

Experimental procedure

Rimmed low-carbon structural steel plates of medium size were welded together. After welding, test strips were cut from the plates to the dimensions shown in Fig. 5. Another group of test bars of the same size, prepared without welding, were notched on either side to a depth of 0·75 mm (0·03 in). The test pieces were clamped to the fatigue testing machine and subjected to cyclic stresses. Figure 6 illustrates the testing arrangement. The test bar was vibrated by means of an oscillator (rotating out-of-balance mass) fixed on one end. The magnitude of vibrations was measured by the well-known Voppel's vibration-measuring triangle. From these results the stresses were computed. The amplitude and, consequently, the stresses of the specimens were varied and the number of cycles to failure was determined. For control purposes the stresses of some test pieces have been measured also by wire-resistance strain gauges.

Table II

Plastics used for coating

Test Series	*Designation*	*Characteristics*
1 and 5b	KGK 19	Epoxy resin—cold worked—without filler
2	EKS 11	Epoxy resin with 40% graphite
3	Polykoll	Polyester with 40% kaolin
4	Liozian	Adhesive on polyurethane-base

Table III

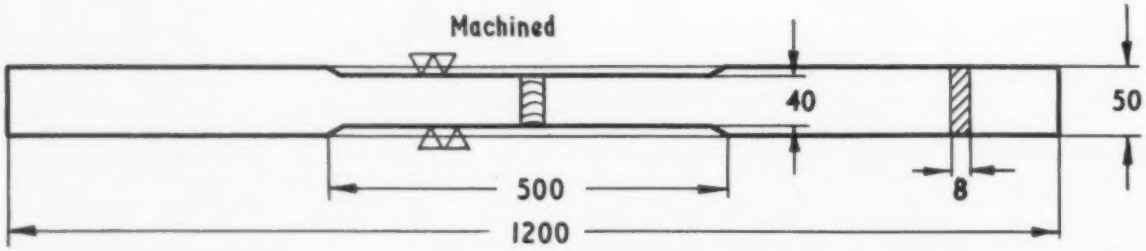
Increase of bending fatigue strength by means of plastic coatings

Test Series	*Plastic Coating*	*Bending Fatigue Strength, kg/sq.mm at 2×10^6 cycles*	*Increase of Fatigue Strength of Plastic Coated Specimens, %*
Notched specimens			
0	—	±3	—
1	EGK 19	±7	133
2	EKS 11	±9	200
3	Polykoll	±9	200
4	Liozian	±9	200
Welded specimens			
5a	—	±8	—
5b	EGK 19	±14	75

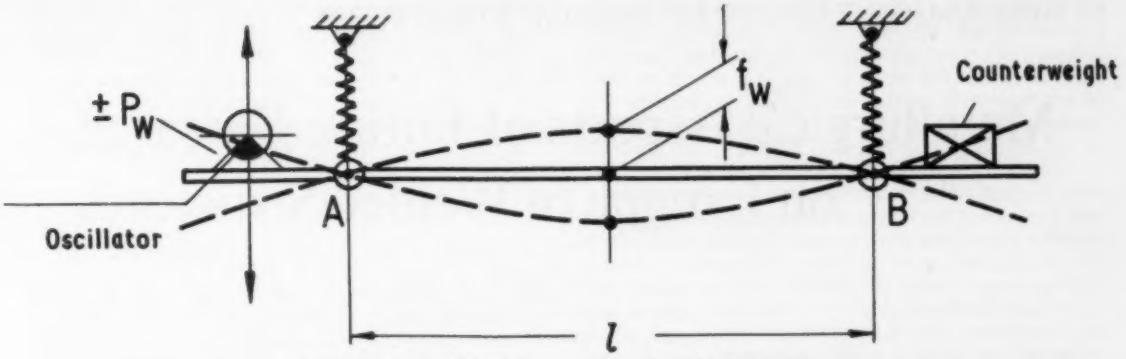
Evaluation of the experiments

The test results were summarized in S/N curves. The aim was not so much to determine the curves accurately but rather to distinguish the differences in bending fatigue strengths. For this reason, not more than 6–12 measurements were made in each test series. The S/N curves were designed and drawn through the experimental points according to a proposal by Neumann,[6] and in compliance with hitherto gathered experiences.

Firstly, the notched specimens were tested. As is well known, a notch has the same effect as a weld bead. However, with a notch, control values can be more easily defined. The S/N curves in Fig. 7 show that the test series 2–4 gave much higher values of fatigue strength than series 0, on which no plastic coating was applied, or series 1 with a coating of EGK 19.

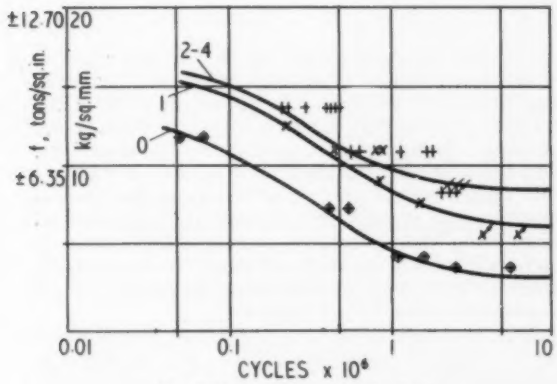


5—Dimensions of test bars

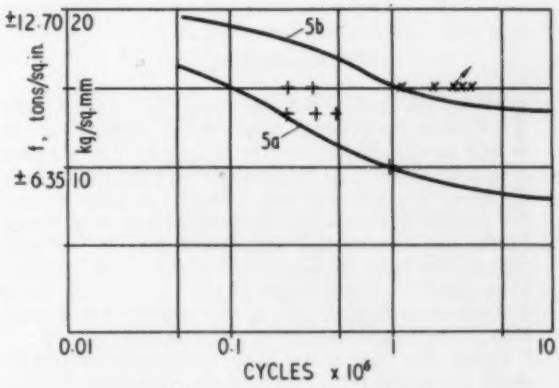


A–B Vibration nodal point
P_w Load
l Length of vibration
f_w Amplitude of vibration

6—Schematic representation of the testing arrangement



7—S/N curves for test series 0–4



8—S/N curves for test series 5a and 5b

With regard to the welded specimens, values are at present available only for those without a coating and with a coating of EGK 19 (Fig. 8). Even here an increase in fatigue strength was observed. Adhesive EKS 11 and others being applied as coatings are expected to give even higher values with welded specimens.

The test results are listed in Table III. It may be seen that for notched bars the fatigue strength can be increased by 200%, whereas for welded specimens the fatigue strength will increase by merely 75%. It is to be hoped that with the application of other plastics an additional increase of fatigue strength will be attained.

Summary

It has been demonstrated that it is possible to increase the bending fatigue strength of notched and welded specimens to a considerable extent by applying coatings of suitable plastics. The method described opens new possibilities for increasing the fatigue strength of dynamically loaded structures in practice. Future investigations of this effect are expected to lead to further increases of fatigue strength.

Acknowledgments

The author wishes to thank his co-workers Mrs. E. Rohl, Mr. H. Schwarz and Mr. G. Muller for their assistance.

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SYMPOSIUM ON FATIGUE OF WELDED STRUCTURES

Metallurgical Aspects of Future Research on Fatigue of Welded Structures

By W. D. Biggs, B.SC., PH.D.

A brief discussion of the mechanism of work hardening in relation to fatigue behaviour suggests that experimental data on the effects of grain size and dispersions are needed. Welded joints form a rather special case in that a notch is effectively present, this notch being located in the heat-affected zone which is, conventionally, of low ductility. An evaluation of possible heat-affected zone structures and of their notched fatigue characteristics is desirable.

Introduction

IT IS an unfortunate but challenging fact that, in spite of much experimental work on the subject, there is still no satisfactory theory of fatigue. The absence of a solid theoretical basis makes the present task at the same time both easy and difficult. It is easy because the results of almost any well conducted experiment on fatigue are a further addition to the store of empirical knowledge; it is difficult because of our inability at present to explain much of the experimental data and hence to suggest critical experiments which may lead to the establishment of a satisfactory theory.

In the first part of the paper some attempt will be made to summarize (as far as may be justified) current ideas on the mechanisms of work hardening and fatigue. So far as is possible only the more generally accepted ideas are put forward, the details of individual fatigue processes being, pusillanimously, neglected. The only justification for this omission is that the mechanisms discussed lead to some proposals for research on the fatigue of welded high-tensile steels—and even though the premises arising from the summary may be wrong it is the author's belief that this does not preclude the need for carrying out tests of the type suggested.

The object of the paper is to stimulate discussion—perhaps a dangerous practice. Many workers may recognize herein snippets of information and opinion as their own brainchildren. To them I offer apologies for not including an exhaustive list of references so that their comments may be recognized and acknowledged, but to do so would have enlarged the paper to a literature survey and such was not its purpose.

Mechanisms of Work Hardening in Metals

Cyclic stressing, leading ultimately to failure, is accompanied by work hardening; there is, as yet, no generalized picture of the work-hardening process that is applicable to all metals but a reasonably consistent pattern appears to be emerging, especially from work on face-centred cubic metals. The process involves the movement of dislocations, which are generated at Frank-Read sources within each crystal and, provided that there is no barrier to the movement of these dislocations, slip occurs at exceedingly low stresses and continues by the repeated generation of dislocations. The crystal finally fractures by a process of 'slipping off', rather like the fracture of Plasticene. But there is clearly some process that prevents the dislocations from being freely generated, a process such that each increment of slip makes it necessary to increase the stress so as to permit deformation to continue. Such a process is termed work hardening.

Even in single crystals of the purest metals some degree of work hardening occurs. This is generally attributed to:

(*a*) The presence of an oxide layer, which prevents the dislocations from escaping at the surface
(*b*) The presence of foreign atoms in the material, which exert a frictional resistance in the slip plane—rather like grit between two sliding surfaces
(*c*) Local inhomogeneities in deformation. These arise because the deformation of the sample produces rotation and bending, which may be restrained by the heads on the specimen
(*d*) The prior existence, within the crystal, of dislocations lying in other planes, thus producing a 'forest' of discontinuities through which the slipping dislocation must move
(*e*) The capacity shown by most of the commercially interesting metals of slipping upon several systems simultaneously; in this case each dislocation generated in one slip plane becomes a barrier to the progress of a dislocation moving on a conjugate plane.

The mechanism of work hardening in polycrystalline metals is very much more complex, and at present comparatively little is known with certainty, but since the crystals must remain coherent, they cannot deform homogeneously on one slip system only. Thus the stage of 'easy glide' is not possible, and many slip systems appear to operate simultaneously so that rapid hardening occurs almost as soon as the flow

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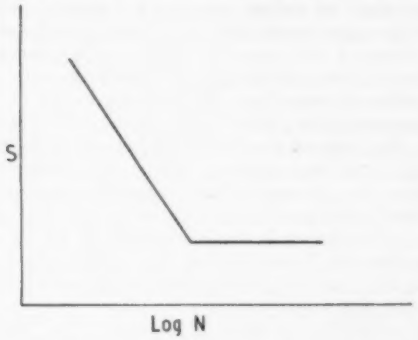
stress is exceeded. This is associated with the presence of grain boundaries in the metal, suitably stable barriers being present *ab initio*. The processes leading to the production of tangled networks of dislocations are thus correspondingly shortened. Clearly, in two-phase alloys such as steels, further barriers are present in the form of carbide particles.

Work Hardening and the S/N Diagram

The conventional S/N diagram for mild steel is of the form shown in Fig. 1. It is clear that, because of the uncertainty surrounding the mechanism of work hardening, there is at present no unequivocal way of describing the deformation processes that operate above and below the so-called 'fatigue limit'.



1—*Conventional S/N diagram for mild steel*

However, before discussing this, it is of interest to examine the significance of the S/N diagram. This consists of a descending arm and a horizontal arm indicating the 'fatigue limit' of the material. It is worth remembering that the location of the descending arm is determined from the results of specimens which have been cycled to failure, whereas the horizontal branch is usually established by means of one specimen which has *not* broken under test*.

Most current investigations suggest that there are, in fact, at least two (and possibly more) mechanisms of deformation at the high and low stress levels that correspond respectively to the descending and the horizontal branches of the S/N curve. At high stress levels deformation is clearly irrecoverable—it is probable that most of the significant damage occurs quite early in the life of the specimen—from 1 to 10% of the life depending upon the experimenter, the material, and the technique. In many commercial testing machines this damage may even be produced before the machine has attained its full velocity. The mechanism appears to include the formation of 'persistent' slip bands, leading to the formation of fissures, which afterwards join up to form a crack; extrusions or intrusions of material displaced by the slip process aid materially in fissure formation. Probably the essential features here are similar to those obtaining in static deformation, *i.e.*, the accumulation of dislocations at barriers within the material creating high localized stresses; for example, at grain boundaries or at particles of a second phase, or both.

At low stress levels the situation is much more complex. Ample experimental evidence exists to show that plastic deformation does, in fact, occur. Such deformation is presumably recoverable; at least it does not lead to fracture even though the damage is cumulative on a microscopic scale, and an abnormally high total plastic strain may be sustained by the specimen. Such distortion is, however, highly localized, and presumably does not involve large piled-up groups of dislocations. Instead, the movement of individual defects (dislocations, vacancies, interstitials, etc.) can occur, and the load is presumably reversed or relaxed before assemblies of a critical size can form.

It may be concluded, perhaps naively, that two factors are worthy of immediate attention:

(1) Fatigue behaviour at low stress levels might be expected to be intimately associated with the stress needed to start the dislocations moving; *i.e.*, if it were more difficult to start deformation one would anticipate an increase in the fatigue 'limit'. There appears to be some experimental evidence that this is so—steels with a higher yield stress show an increased fatigue limit. Again, in passing, one would think that the relationship between yield stress and fatigue limit should be at least as good as (and much more sensible than) the empirical ratio fatigue-limit/ultimate-tensile-strength. This is certainly true in hardened and tempered alloy steels. It is, perhaps, unfortunate that in Britain enhanced tensile strengths are not obtained primarily by the use of alloying elements which harden the ferrite by solid solution. The following is a list of such elements together with approximate figures (drawn from various sources) of the increase in yield strength that may be obtained by their use.

Element	*Approx. Increase in Yield Strength per 1% of Alloy, tons/sq.in.*
P	40–50
Be	35–45
Si, Mn	6–8
Ni, Ti	4–6
Al, Mo	2–3
W, V	1
Co, Cr	1

It appears to be fairly common practice to use higher silicon contents in Continental steels, but unfortunately the fatigue test data on such materials are confused, and it is desirable that they should be critically reviewed and that some further experimental work should be carried out.† The fatigue properties of unwelded and welded specimens should be determined on several steels

* One may, in passing, query the validity of the conventional plot, which includes specimens of such differing behaviour that they might well be different materials. The results are, in fact, drawn from two different statistical populations and the use of a fatigue 'limit', *i.e.*, of a limiting safe stress, is conceptual only and its usage must be attended with some caution. See also A. M. Freudenthal: "The Inelastic Behaviour of Engineering Materials", John Wiley & Sons Inc., 1950, pp. 560 *et seq.*

† Particular attention might be paid to the loading conditions. It is possible that the contradictory nature of much of the evidence may be due to differing responses to the load pattern on the part of the various microstructural constituents.

having the same yield strength. The steels should cover as wide a range of solution hardening elements as is practicable but should be otherwise conventional in composition.

(2) Grain size is clearly of great importance. If the development of a crack be associated in any way with blocked dislocations it is clearly necessary that the stresses around the tip of the pile-up be kept low. It has frequently been shown that the yield strength is increased by decreasing the grain size. This effect is ascribed to the difficulty, in fine-grained materials, of achieving a piled-up group of sufficient magnitude to create a high stress on the leading dislocation so that slip does not propagate readily from grain to grain through the metal.

Unfortunately the effects of grain size upon the fatigue properties have received but little unequivocal study; the normal methods of varying the grain size of steels by heat treatment are invariably accompanied by variations in microstructure (especially in the distribution and morphology of the pearlite) so that the effects of grain size alone are difficult to assess. In general, however, it is considered that a coarse grain size is associated with inferior mechanical properties, and the fatigue characteristics appear to be similarly affected. Two deficiencies in the published literature are at once apparent:

(i) There are few reliable data on the amount of grain coarsening produced by welding cycles under controlled conditions

(ii) Comparative fatigue tests on samples of differing grain size are vitally necessary to determine how much grain size (in the absence of other microstructural modifications) is, in fact, a significant factor in determining the fatigue characteristics of steels. For ease of interpretation it would be preferable to carry out preliminary tests on a single steel, whose grain size has been modified by heat treatment, before investigating other steels.

(*a*) Data are needed to assess the effect of welding on 'fine grained' (aluminium killed) and conventional steels. It is particularly necessary to know more about the behaviour of the heat-affected zone of fine grained steels when a stress raiser is present as, for example, in a fillet weld or an undressed butt weld.

(*b*) By a similar reasoning one would anticipate that, since the flow stress of steel is intimately bound up with the amount and distribution of the carbide phase, the fatigue life would also be affected. Here again data are available for steels which have received conventional heat-treatment cycles but comparatively little is known about the fatigue life of the rather unorthodox dispersions of carbide produced in the heat-affected zone. Experimental work in this field is urgently needed.

Fatigue of Welded Joints

It is generally agreed that the fatigue characteristics of a component are dependent, primarily, upon geometry; the effects of notches and other discontinuities are quite well established. For most practical purposes a weld constitutes a notch of some kind—this is obviously true for a fillet weld and also of an undressed (*i.e.*, as welded) butt seam. It is less obviously true for a weld that has been ground flush with the plate. Yet it is frequently found that, associated with the weld, there exists one or another form of mechanical defect (undercutting, internal defects such as lack of fusion, etc.) from which a fatigue fracture may initiate. In fact, one of the saving features of welding for applications involving cyclic stressing is that it is often possible to prophesy the probable location of a fatigue fracture. But there is little evidence to suggest that a truly homogeneous weld deposit would be expected to have fatigue properties inferior to those of the parent plate—provided that sufficient care is taken in preparation and inspection, a welded mild steel specimen in which the surface has been machined quite smooth will generally fail in the parent plate.

Such failures are usually, although not invariably, located in the heat-affected zone, and in general a low-temperature annealing operation ('stress relieving') does not significantly affect the location of the fracture. Against this may be cited a large number of specimens which fail in the plate well away from the heat-affected zone. Such failures appear to be more common in specimens tested at relatively high strength levels. The reason for this behaviour is undoubtedly the same as that giving rise to the characteristic 'fracture-in-plate' behaviour under tension loading: the lower yield point of the plate ensures that plastic deformation tends to be concentrated in the plate rather than in the weld or in the heat-affected zone. In such cases it is fair to suggest that the weld itself has often not been tested at all.

The effects of the arc-welding cycle upon the structure of alloy steels may give rise to any or all of the following features in the heat-affected zone:

(i) The grain size is greatly increased
(ii) Inhomogeneous austenite
(iii) Non-equilibrium microstructures (martensite, bainite etc.) or mixed structures characteristic of slack quenching may be obtained.

Published data on these factors are limited and are virtually non-existent for steels in the tensile strength range of primary interest here.

Inhomogeneity of Austenite

There is ample evidence that the rapid heating of an arc-welding cycle does not permit the development of an austenitic structure as homogeneous as that obtained in conventional heat-treatment practice. Two possibilities may be considered here:

(*a*) Carbides, especially the more stable alloy carbides (*e.g.*, of chromium, molybdenum, etc.) may not be fully dissolved. On subsequent cooling it would be anticipated that pearlite rather than martensite would be nucleated by such residual carbides, resulting in 'split transformation' products and mixed structures.

(*b*) Even if carbides dissolve there may be insufficient time at peak temperature to permit full diffusion of carbon, with consequent homogenization of austenite. At the high temperatures involved this is, perhaps, unlikely, but any delay in carbide solution will in turn delay diffusion of carbon.

These features do not, perhaps, merit investigation so far as the immediate problem is concerned, although they are clearly of long-term interest. It would

certainly be desirable to have some information on the ease of solution of a range of alloy carbides under the heating and cooling conditions imposed by arc welding operations, and on the effects of incomplete solution and of inhomogeneity upon the type and amount of the transformation products.

Formation of Non-Equilibrium Structures

The most significant feature of high-tensile steels is, however, their tendency to hardening by rapid cooling of the austenitic phase. Although the composition is generally arranged so that no special precautions are necessary during welding, weldable high-tensile steels can nonetheless develop martensitic or bainitic structures after welding. These structures are subsequently tempered by successive runs and may be further tempered by low-temperature annealing (stress relieving) after welding.

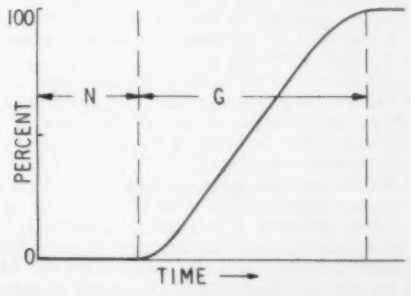
The danger of fully hardened (martensitic) structures is well known as an important contributory factor in hard-zone cracking. Clearly, the existence of such cracks, even though they be microscopic in scale, must encourage premature fracture in the heat-affected zone. For the present purpose any steel or welding procedure in which a fully martensitic heat-affected zone is produced, may be dismissed.

Thus the following microstructural constituents may be present in the heat-affected zone:

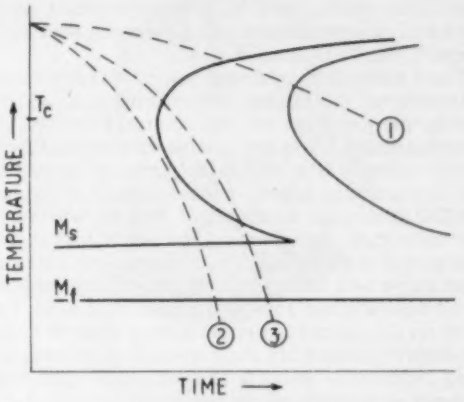
(*a*) *Single run welds:* Ferrite, cementite, pearlite, bainite, martensite.
(*b*) *Multi run and/or heat-treated welds:* The above constituents tempered, plus the transformation leading to temper brittleness in the material.

In view of the theme of the present symposium perhaps it may not be considered an impertinence, in a meeting in which engineers predominate, to explain how these structures are produced.

Structures such as ferrite and pearlite involve a process of nucleation and growth. Their formation is, therefore, time-dependent and the structures may be obtained by relatively slow cooling or by isothermal reaction of the austenite. Nucleation and growth processes are characterized by a reaction-rate curve of the type shown in Fig. 2, where a nucleation period N is followed by a period of growth G. If such curves are determined by isothermal reaction at various temperatures the start and finish of the transformations gives the characteristic TTT (time-temperature-transformation) curve shown in Fig. 3. If a steel is cooled so rapidly that it reaches the critical temperature T_c in a time that is too short to permit of nucleation, the austenite → ferrite change occurs at low temperatures ($M_s \rightarrow M_f$) to produce martensite by a shearing process that is virtually instantaneous.



2—*Diagrammatic curve of an allotropic change involving nucleation and growth*

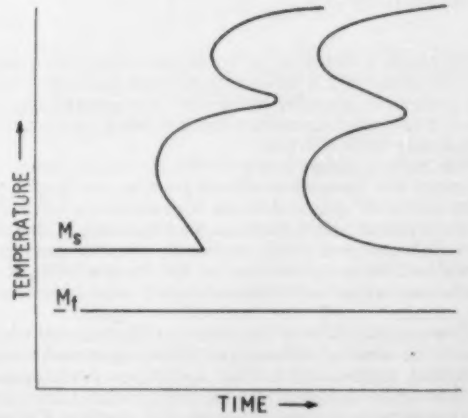


3—*Diagrammatic time-temperature-transition (TTT) curve indicating type of relationship between isothermal and continuously cooled transformation data*

Thus the following combinations are possible in a steel having a TTT curve of the type shown:

(1) Slow cooling, producing coarse pearlite (and pro-eutectoid ferrite depending on the carbon content)
(2) Rapid cooling (critical cooling velocity), producing martensite
(3) Intermediate rates of cooling, which produce mixed structures consisting of fine pearlite (with or without pro-eutectoid ferrite) and martensite.

Bainitic structures (having properties intermediate between pearlite and martensite) are not readily produced in plain carbon steels except by isothermal transformation. It is conceivable (though perhaps unlikely) that a cooling cycle of this type may be produced in welds where a very high heat input coupled with preheating precludes cooling to below M_s. In



4—*Modified TTT curve characteristic of many alloy steels*

many alloy steels, however, bainitic products may be produced on continuous cooling owing to the different shape of the TTT curve (Fig. 4).

There are several published papers which give some indication of the fatigue characteristics of materials having many of these microstructures. However, there do not appear to be many reliable data on steels whose tensile strength falls within the range of interest here and, in particular, there is a great scarcity of results on notched specimens which might indicate whether one microstructure was more or less notch sensitive and thus permit of an evaluation of which type of structure is desirable and which may be potentially dangerous in the heat-affected zone of an undressed weld. Some work on this aspect of the problem is now on hand in the author's laboratory but a great deal remains to be done before it is possible to make more than a preliminary assessment of the situation.

Conclusions

It must be emphasized that, in the author's opinion, it is not enough merely to weld large pieces of steel together and test them in order to achieve some measure of understanding of the behaviour of welded joints under cyclic loading conditions. Such an empirical approach has served far too badly for far too long, and usually results in a failure which has been precipitated by that structural element which is the most sensitive to the particular set of loading conditions imposed on it. It is a source of continual amazement that so little is known with certainty about the heat-affected zone of a weld—even in mild steel. Half an hour with a good microscope should convince even the most doubting observer that the finer details of the structures produced in this region are not those which are normally to be found in text books on metallography. And fatigue is essentially a phenomenon which depends upon localization of flow and upon the fine details of structure.

Thus the problems outlined in the foregoing will require a careful study of heat-affected zone structures and the development of techniques to simulate these structures in a fatigue test piece.

Finally, it is perhaps permissible to conclude by dragging one more skeleton from its hiding place. The relationship between fatigue strength and tensile strength is clearly empirical in nature—and the use of the strongest material is not necessarily the correct solution to a given design problem. The introduction of a notch in a steel component invariably results in a reduction of its fatigue life, but this is often much less disastrous in low-strength than in high-strength steels. In practice, a weld constitutes a notch, so is it fully justified to seek steels of high strength when the ductility may in fact be of equal or even greater importance? Unfortunately, ductility data are always incomplete: conventional testing practice gives only the elongation and reduction in area, neither of which are accurate or meaningful, depending, as they do, upon shape and gauge length and telescoping into one measurement the uniform and localized deformations of the material. At best they can be used only as standards for comparing one steel with another, whereas a more extensive knowledge of the ductility (separated into uniform and localized fractions) would probably be of material benefit in attempting to assess the response of the steel to cyclic loading conditions.

Book Review

A. Erker, H. W. Hermsen, A. Stoll: "*Design and Calculation of Welded Constructions*" ("Gestaltung und Berechnung von Schweisskonstruktionen"), Dusseldorf, 1959, Deutschen Verlag fur Schweisstechnik (DVS), 180 pp. (15.80 DM).

The book is meant as an introduction to welded design for the novice and it serves this purpose admirably within the limitations imposed by its size. The experienced man too will find much to interest him and many practical hints which may be new to him.

The book is meant primarily for the draughtsman, and 36 pages are devoted to drawing office specifications in addition to 37 pages devoted to calculation of welded joints, whereas only 6 pages at the beginning of the book deal with equipment and processes; this can scarcely be considered adequate information for the man who, in the course of design, has to decide which process is to be used for joining.

Of particular value is the extensive and rational discussion of the merits of different joint preparations and possible difficulties experienced in their application. Some space is devoted to the design of structural connections and joints, and connections in pressure vessels and pipelines. Different forms of nozzle connection are described but in this, as in other chapters dealing with what might be termed broadly structural connections, one rather misses some discussion on stress distributions and relative difficulties and problems experienced in execution.

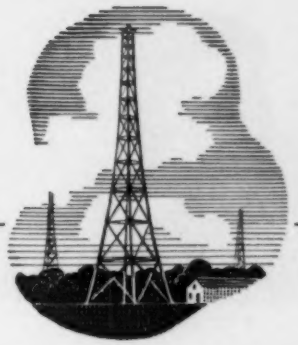
The emphasis of course in a book of this size must necessarily be on the "How" and not on the "Why".

Fatigue is considered in some detail and it is interesting to note that Gassner's ideas on cumulative damage have to some extent been incorporated. It is unlikely that any young designer who has studied this chapter carefully would be likely to make the kind of serious mistake that at present is still far too common in structures expected to withstand fatigue loading.

There are at the end of the book extensive excerpts from relevant German specifications and there is no doubt that the book gives a good picture of German ideas on design and German practice. In a way it is a pity that one cannot see much use for a translation of this book into English because British and American practice differs a great deal from that in Germany. For one thing the designer in Britain and in America is not quite so severely circumscribed by regulations, and has considerably more freedom. Standards in Germany have the force of law and must be adhered to. Nevertheless a book in English on somewhat similar lines would be a great asset.

R. Weck

NEWS
and Announcements



News of the Institute and Branches
B.W.R.A. · Other Societies ·
and Industry

INSTITUTE ACTIVITIES

SPRING MEETING 1960

The following papers are promised for the Spring Meeting of the Institute this year, which is to be held at Droitwich from 9th to 11th May inclusive:

"Electron Beam Welding", by M. E. Harper
"The New Rochelle High Frequency Resistance Welding Process", by Dr. F. J. Trotter
"Metal Spraying", by J. R. Gault and G. A. Curson
"Plasma Hard Facing", by E. A. Gerhold
"Automatic Production Metallizing of Machinery Components", by Lloyd Manuel
"Fine Wire Welding", by J. A. Lucey, D. B. Tait, and R. V. Brimble
"Electro-slag Welding", by J. A. Lucey and D. B. Smout
"Electro-slag Welding", by W. K. B. Marshall
"Electro-slag Welding", by A. M. Horsfield
"Sprayed Metal Coatings for Abrasion, Corrosion and Oxidation Resistance", by G. R. Bell
"Droplet Transfer in CO_2 Welding", by Dr. van der Willigen

As many as possible of these papers will be published in the April issue of BWJ.

Details of the works visits and of the programme generally will be circulated to members as usual early in March.

Welding and the Design of Ships

Mr. W. G. John, R.C.N.C., M.I.N.A., will deliver the **Third Annual Lecture** at the Institute of Welding on Thursday, **3rd March,** at 6.30 p.m. His subject will be the influence of welding on the design of ships. Admission to the lecture will be by ticket, obtainable from the Secretary.

WELDING EDUCATION

The President of the Institute has set up a special committee on educational policy, composed as follows:—

Chairman; the President (Mr. E. Seymour-Semper)
Professor J. G. Ball
Mr V. W. Clack
Mr. E. Fuchs
Dr. N. Gross
Dr. E. F. Gibbs
Dr. R. Weck
Mr. R. E. G. Weddell

The Committee held its first meeting in January, and is engaged in formulating proposals for an advanced course for the training of welding technologists, because several technical colleges and universities have recently shown interest in the possibility of a postgraduate welding course, and there is increasing evidence of industry's need of highly qualified welding technologists.

PUBLICATIONS

Index

The combined subject and name index, and preliminary matter, for volume 6 of the *British Welding Journal*, covering the twelve issues from January to December 1959, was distributed with the February issue of *B.W.J.* Additional copies, if needed, may be had on application to the Secretary, Institute of Welding, 54 Princes Gate, London, S.W.7.

"The Electric Arc in Welding"

Reprints of the series of papers on "The Electric Arc in Welding", printed in the February issue (pp. 71–128) and including the colour plate, are available from the Secretary of the Institute at 7*s.* 6*d.* a copy.

NEWS OF MEMBERS

Mr. David Waters, A.M.Inst.W., has resigned his appointment as Pipe Welding Superintendent to Land and Marine Contractors, Ltd., Bromborough, and has been appointed Chief Welding Engineer to Inspection Services, Ltd., Sutton. Mr. Waters helped to pioneer the application of ultrasonics as a medium of pipeline inspection in conjunction with radiography, and as part of his new duties he will be responsible for future applications of the technique on pipelines at home and abroad.

Mr. S. G. P. de Lange, who spent 23 years in Britain with Philips Electrical Ltd., has been appointed Commercial Co-ordinator with the Nellen's Machinefabriek, Constructiewerkplaats and Technisch Bureau N.V. in Rotterdam.

During his years with Philips Electrical Ltd., from which he retired last year, he was for a considerable time Commercial Manager of the Industrial Products Division.

Mr. W. G. Beynon, M.Inst.W., has recently returned from Pakistan and has taken up an appointment at the Department of Materials Research, Admiralty, in London.

Mr. S. E. Witherington has left Messrs. Hoskins & Sons, of Bordesley, Birmingham, and recently took up an appointment with Suffolk Iron Foundry (1920) Ltd., as their Birmingham area representative.

Dr. M. C. Nickson, who was in charge of the metallurgical department of G. A. Harvey & Co. (London) Ltd., has resigned and has joined the Dunlop Rim & Wheel Co. Ltd.

Mr. C. R. Thatcher has relinquished his post as area manager with the Lincoln Electric Co. Ltd.

Obituary

Mr. A. C. Hartley, C.B.E., President of the Institution of Civil Engineers, died in a London hospital on 28th January, 1960. Mr. Hartley was a Vice-President of the Institute of Welding in the year 1958–9, but after undergoing a major operation he resigned from his office early in 1959.

Arthur Clifford Hartley was born in 1889, educated in Hull, and from the Hull Technical College went to the Imperial College of Science and Technology, where he took his A.C.G.I. and his B.Sc.(Eng.) in 1910. He then became

K. W. Gunn

R. McLester

a pupil of Mr. T. M. Newell, Chief Engineer, North Eastern Railway, Hull Docks. During the war of 1914–18, he took a commission with the Royal Flying Corps, and distinguished himself, graduating as a pilot, being appointed O.B.E., and twice being mentioned in despatches.

After the First World War he became a partner in the consultant firm of Maxted and Knott, but it is chiefly for his long and successful connection with the oil industry that he will be remembered among engineers. Ten years after his appointment to the Engineering Department of the then Anglo-Persian Oil Company in 1924, he became Chief Engineer, which post he held until his retirement in 1951, when he returned to private practice and as a consultant to Messrs. Rendel, Palmer and Tritton.

His services during the Second World War included the development of a stabilized automatic bomb-sight, and a major part in the "Pluto" project, the pipe-line which carried oil across the English Channel. In recognition of his share in the inspiration and execution of this most important project he was awarded £9,000 by the Royal Commission on Awards to Inventors. As Technical Director to the Petroleum Welfare Department, Mr. Hartley was responsible also for the development of "Fido"—the fog clearance system for airfields, and for flame weapons. He was appointed C.B.E. in 1944.

Mr. Hartley was President of the Institution of Mechanical Engineers in 1951. He was also a member, and some time Vice-President, of the Institute of Petroleum.

The Institute has lost in him one of its most distinguished members, and one of the best loved.

CONTRIBUTORS TO THE JOURNAL

Mr. K. Gunn, head of the Engineering Research Division of Aluminium Laboratories Ltd., Banbury, graduated in Metallurgy at the Royal School of Mines, Imperial College, London, and was subsequently employed in Northern Aluminium Co. Ltd.'s Research Dept. In 1946 he was transferred to Laboratories, when the Banbury laboratories were re-opened after the war.

Mr. R. McLester is head of the Fatigue Section of the Engineering Research Division of Aluminium Laboratories Ltd., Banbury. In 1955 he graduated in Mechanical Engineering from Manchester College of Science and Technology (Faculty of Technology in the University of Manchester), and has since been employed at Laboratories.

BRANCH NEWS

Manchester

Members and visitors at the fourth meeting of the Branch, held at the Manchester College of Science and Technology on 2nd December, plied a panel of experts with many interesting problems. The panel consisted of F. Koenigsberger, E. J. Heeley, R. Bushell, and E. H. Lee.

Future trends of the industry, and automation in particular, were subjects of considerable discussion, and it was the opinion of the panel that automatic processes, the exact type of which was not defined, would, with increased speeds and lower costs, 'fill the bill' to some extent. Full automation would have to be taken into account at some time. Many other questions were put, among them the introduction of new electrodes in the manual field, magnetic flux electrodes, semi-automatic processes, and inert-gas welding.

All these were answered very competently and it seems likely that this form of meeting will become a permanent feature for future Branch lecture programmes.

A.C.W.

63 ft. long underframe for railway rolling stock

West of Scotland

A meeting of the Branch was held at the Institution of Engineers in Glasgow on Wednesday, 16th December, 1959, when a paper entitled "*British Railways —Welding of Rolling Stock*" was presented by Mr. G. Walton of British Railways Workshops, Derby. Mr. J. E. Jones, Past Chairman of the Branch, presided in the absence of Mr. D. B. Kimber.

Mr. Walton traced the development of welding from the early days of minor welding repairs through the era of steam to present-day developments for diesel and electric propulsion.

Materials used to British Railways specification are all tested before use to eliminate the possibility of wastage in manufacture due to defects.

Joint preparation is of major importance, and the lecturer instanced many faults in this respect in industry as a whole. He pointed out that time and costs are doubled by poor preparation.

At Derby and elsewhere, the welders are tested regularly on test pieces similar to the work engaged upon, in full co-operation with the Inspection Department. This ensures a high standard of work on parts subject to severe live stresses in service. In this connection, Mr Mr. Walton showed various slides of

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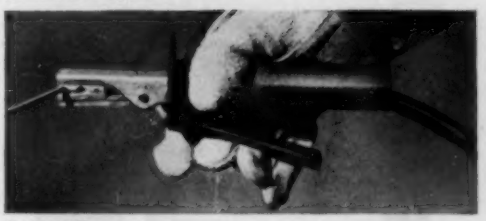

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NI-O-NEL* *corrosion-resisting alloy*	65 NI-O-NEL
CORRONEL* 210 CORRONEL* 220 — *corrosion-resisting alloys*	CORRONEL 210

MATERIAL	ARGON-ARC FILLER-WIRE
CORRONEL* 230 *corrosion-resisting alloy* INCONEL* *oxidation-resisting alloy* INCOLOY* *heat-resisting alloy*	CORRONEL 230 NC-82 INCO-WELD 'A'*
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such parts, and the accompanying photograph shows a 63 ft. frame accurate to $\frac{1}{16}$ in. on length.

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Many points were raised during the ensuing discussion, which were most ably answered by Mr. Walton.

At the close, Mr. J. E. Jones expressed the appreciation of members to Mr. Walton for his very topical paper.

H.H.M.

N.E. Tyneside

Annual Dinner

The Branch, at its Annual Dinner in the Old Assembly Rooms, Newcastle upon Tyne, on Saturday, 5th December 1959, had a good attendance of members and their guests. The Chairman of the Branch, Mr. R. A. Exton, presided and proposed the toast of the Institute, the response being made by the President, Mr. E. Seymour-Semper. Mr. Christopher Stephenson proposed the toast of the Guests, to which Mr. E. J. Hunter, Chairman of Swan, Hunter and Wigham Richardson, Ltd., and President of the North East Coast Institution, replied. The health of the Chairman was proposed by Mr. M. E. Pool, Vice-Chairman of the Tees-Side Branch.

North London

The British Oxygen Engineering, Ltd., of Angel Road, Edmonton, were the host to the Branch on 16th December, when over 100 members and visitors were received for demonstrations and a film.

The evening started with a colour film showing the latest gas cutting machine, which is operated by a magnetic tape on the lines of a tape recorder. The preparation of a tape, followed by the actual cutting operation being controlled by the tape was most impressive.

This was followed by a tour of the works, where demonstrations had been laid on, covering the automatic welding of fillets and butts, and an interesting newcomer—the Quasi-Arc *Sigmette*. The speed of welding possible with this machine amazed the members who saw it demonstrated on standing fillets.

A vote of thanks was made by the Branch, for a most instructive and well organized evening, which all had enjoyed.

R.B.W.

South London

On Wednesday, 13th January, the Branch received a most interesting lecture by Mr. E. R. Perry, Development Officer with the Mond Nickel Company. The subject was "High Temperature Brazing: A Modern Joining Method", and Mr. Perry mentioned the very earliest applications of brazing, which apparently occurred in the Egyptian era. He then discussed in detail the range of alloys that are now available to users, stressing the great improvement in properties that can be achieved by the addition of certain precious metals such as palladium. Quoting figures for stress rupture, shear, and creep, Mr. Perry indicated important aspects of design. A long list of heating methods was illustrated with slides, and members were interested to see slides showing the wide range of products that are currently being brazed in the gas turbine and radio field. Specialized applications are in the brazing of metals to ceramics and in the sealing of nuclear cans. The important aspects of cleaning, type of joint, and preparation of the work were also raised. The meeting closed after a brief discussion and a vote of thanks.

N.B.

Birmingham

Annual Dinner

The Branch Annual Dinner was held at the Grand Hotel, Birmingham, on Thursday, 3rd December, 1959, with the Branch Chairman, Mr. M. Riddihough presiding, and The Right Worshipful the Lord Mayor of Birmingham, Alderman J. H. Lewis, O.B.E., J.P., as the principal guest.

The ballroom was filled to capacity with over three hundred members and their guests. The toast of The City of Birmingham was proposed by Mr. A. J. B. Owen, C.B.E., and the response was made by the Lord Mayor. Mr. F. Garner proposed the toast of the Institute of Welding and the Birmingham Branch, to which the President of the Institute, Mr. E. Seymour-Semper, responded. The Chairman proposed the toast of the Guests and the response was made by Dr. W. I. Pumphrey.

Leeds

Annual Dinner

The principal guest at the 17th Annual Dinner of the Branch, held at the Hotel Metropole on 10th December last, was Councillor A. R. Bretherwick, Deputy Lord Mayor of Leeds. The Chairman of the Branch, Mr. R. Gardener, presided over a large gathering, which included Mr. J. Alcock, President of the Leeds Association of Engineers, Mr. E. G. Haigh, President of the Keighley Association of Engineers, Mr. A. J. Butterfield, Past President of the Leeds Branch, Mr. S. Stansfield, Managing Director, Oxley Engineering Co. Ltd., and the President of the Institute, Mr. E. Seymour-Semper, who responded to the toast of the Institute of Welding, proposed by the Deputy Lord Mayor, stressing the importance of advanced education in welding. The toast of the guests was proposed by Mr. Eric Rose, Vice-Chairman of the Branch, in the regretted absence of his father, Mr. A. E. Rose, Branch President, who was indisposed. Mr. A. J. Butterfield replied on behalf of the guests.

South Western

Annual Dinner

There was a good attendance of nearly 100 members and guests at the Annual Dinner of the Branch, which was held at the Berkeley Restaurant, Bristol, on 18th December, 1959. In the regretted absence of the Branch Chairman, Mr. T. M. B. Sessions, the Chair was taken by Mr. F. J. Wilkinson. Mr. A. J. Francis, the Branch President, proposed the toast of the Institute of Welding, to which the President of the

Members and guests at the dinner of the South Western Branch

Institute, Mr. E. Seymour-Semper, responded. The Chairman then proposed the toast of the guests, and the response was made by Sir A. William Grant, C.B.E., Chairman, Engineering and Allied Employers West of England Association.

BRITISH WELDING RESEARCH ASSOCIATION

Engineering Materials and Design Exhibition

The British Welding Research Association's stand at the Engineering Materials and Design Exhibition, held on 22nd–26th February, emphasized one aspect of the Association's work—the correct design of welded components. To obtain full economic advantage from welding, with safety, designers must ensure that the welds are fully and correctly specified on drawings and must pay attention to a few simple but important requirements. Some of these were illustrated by four simple sets of model structures, one showing the detail commonly used for attaching branch and reinforcing plate to a cylinder; one weld is often almost inaccessible. An improved detail was shown which allows better access for making the welds, and also a very much simplified arrangement with no reinforcing plate which has been shown by research to be better than the conventional method and uses considerably less material.

The other models showed the right and wrong detail designs for heavy plate girders, such as crane gantry girders; for intersecting girders used, for example, in welded entablatures; and for a fabricated bearing pedestal.

A senior member of the technical staff of the Association was in attendance to answer queries on welding techniques and designs for welded fabrications, and a full range of the Association's publications were exhibited.

Plastic Properties of Rolled Sections

The Association has recently published a new work of reference on the plastic moduli of steel joists, beams, and channels.

Compiled by Dr. M. R. Horne in the course of work on the plastic theory of structures being carried out at the Engineering Department, University of Cambridge, "*The Plastic Properties of Rolled Sections*" deals comprehensively with the plastic moduli, with and without load, of BS. rolled steel joists, broad flange beams, universal beam sections, channels and special channels. A further section is devoted to the properties of BS. equal and unequal angles.

The booklet contains fully explained tables, in part extracted from BS.4:1932. Algebraic expressions for the various properties quoted, with details of their derivation, are given in three appendices.

Advice on the content and form of the work was given by the D.1 (Load Carrying Capacity of Frame Structures) Committee of BWRA and new information, given by The Bethlehem Steel Co., U.S.A., The South Durham Steel and Iron Co., and Dorman Long and Co., is also included.

Copies of this work, price 8*s*. 6*d*. are obtainable from the Publications Department, British Welding Research Association, Abington Hall, Abington, Cambridge.

OTHER SOCIETIES

Metal and Plastic Coatings Association

The 10th Annual General Meeting of the Metal and Plastic Coatings Association was held at the Century Hotel, Wembley, on Tuesday, 1st December. The meeting started with a demonstration of Spray Fuse and Spray Weld flame-sprayed Stellite coatings by Delori Stellite Ltd. and F. W. Berk & Co. Ltd. Mr. J. R. Gault of Deloro Stellite gave a short talk on the properties and applications of the coatings, which are mainly in the hard-surfacing fields; the coatings also have excellent anti-corrosive properties.

This was followed by a film entitled "*Metals in the Atomic Age*" which was shown by arrangement with the Atomic Weapons Research Establishment, Aldermaston.

Dr. J. C. Hudson of the British Iron and Steel Research Association then gave a talk on the corrosion of iron and steel illustrated by slides. Dr. Hudson developed the theory of corrosion of iron and steel in various atmospheres and gave examples of how the corrosion rate would be reduced with the application of coatings by galvanizing, metal spraying, and painting. This led to the conclusion that zinc coatings can play a major part in the prevention of iron and steel corrosion.

At the Annual General Meeting, it was agreed that Mr. J. R. Gault and Mr. G. A. Curson should give a combined paper on metal spraying at the Institute of Welding Spring Meeting at Droitwich, and also that the Association should have a stand on which its work could be illustrated. Dr. T. P. Hoar will present a paper on behalf of the Association at the International Welding Colloquium in Liège. It was also agreed at the meeting that the scope of the Metal and Plastic Coatings Association should be extended considerably so that all makes of protection equipment and users are eligible for membership; thus the Association would be able to represent all the metal spraying and plastic coating interest at a technical level. The Technical Committee will be set up to supervise and expand the research and development work undertaken by the Association and to ensure proper collaboration with other technical bodies, such as the Institute of Welding Metal Spraying Committee and the British Iron and Steel Research Association and the British Standards Institution.

Non-destructive testing

A meeting of the Non-Destructive Testing Group of the Institute of Physics will be held jointly with the Société Française de Métallurgie on 2nd–4th May.

The programme will have the general theme of the relationship between structure and physical properties of materials, and will include recent advances in non-destructive testing techniques. Further details can be obtained from the Secretary, The Institute of Physics, 47 Belgrave Square, London, S.W.1.

Determination of gases in metals

The Society for Analytical Chemistry is organizing, in conjunction with the Iron and Steel Institute and the Institute of Metals, a joint meeting on the "Determination of Gases in Metals".

It will be held at Church House, Great Smith Street, London, S.W.1, on 3rd–4th May. It is intended that preprints of the papers to be presented will be available. Those interested should communicate with one of the Secretaries of the three societies.

Fluid power

The First International Compressed Air and Hydraulics Exhibition is to be held on 25th–29th April at the Empire Hall, Olympia in association with the Second European Fluid Power Conference.

It is sponsored by the monthly journal *Compressed Air and Hydraulics*.

AWS Annual Convention

The 41st Annual Convention and Welding Exhibition of the American Welding Society will be held in Los Angeles, California, on 25th–29th April.

Technical meetings will be held at the Biltmore Hotel, and the Exhibition will be in the Great Western Exhibit Centre.

Research Scholarship

A research scholarship in the use of light alloys in structural engineering is offered every third year by the Institution of Structural Engineers, in collaboration with the Aluminium Development Association. The duration of the scholarship is two years and the value is £600 per annum.

The next award will be made this year, to date from 1st October, 1960.

Details and application forms are obtainable from the Secretary of the Institution of Structural Engineers. Completed application forms should be sent in by 31st March, 1960.

NEWS FROM INDUSTRY

New Divisions of A.E.I.

On January 1st, Siemens Edison Swan Ltd. changed its name to Associated Electrical Industries (Woolwich) Ltd. The new Company is responsible for managing four new Product Divisions of Associated Electrical Industries Ltd., which are A.E.I. Telecommunications Division, A.E.I. Radio and Electronic Components Division, A.E.I. Cable Division, and A.E.I. Construction (Cables & Lines) Division.

In addition, the following companies are now grouped under the management of Associated Electrical Industries (Woolwich) Ltd.: The London Electric Wire Co. and Smiths Ltd.; A.E.I. Plastics (Aldridge) Ltd. (formerly Aldridge Plastics Ltd.); Henley Foundries Ltd.; The Fixed Price Light Co. Ltd.

Spot welding demonstrated

A Conversazione of the Parliamentary and Scientific Committee was held at The Royal Society on 9th December. Among the exhibits was the model of a vehicle showing new uses and treatment of metals in road transport. Resistance spot welding had been used for the assembly of the shell.

NEW PLANT AND EQUIPMENT

Portable welding unit

Theranews Imports have recently developed a small-size portable welding unit, the "Troll" Type MS13, which has a welding current range of 55–130 amp with 4 intermediate stages. The unit weighs 70 lb and the standard version can be connected to any 220V, a.c. supply. The maximum continuous manual welding period is about two hours.

Welding demonstration workshop

A demonstration workshop has been opened by Meritus (Barnet) Ltd. at their

factory at Barnet, Herts. It displays a comprehensive range of the Company's resistance welding machines, which can be demonstrated (by appointment) at any time, including Saturdays.

Lightweight mobile a.c. welding plant

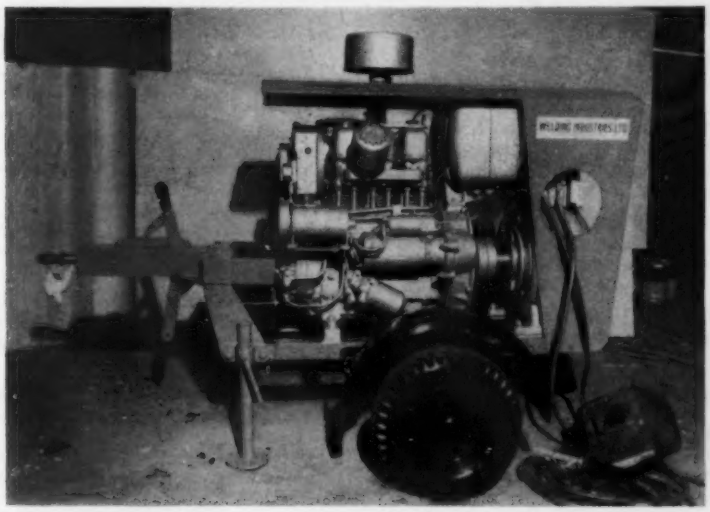
Welding Industries Ltd. recently developed a new welding plant which was exhibited at the Building Exhibition, Olympia, 1959. This plant is of a revolutionary design, embodying a medium frequency a.c. alternator, driven by a Petter P.C.3 diesel engine which develops 15 h.p. at 3,000 r.p.m.

The total weight of the plant, mounted on high speed undergear is approx. 855 lb, and the dimensions are: length (inc. towbar) 6 ft 4 in., height 4 ft 2in., width 3 ft 5 in.

The plant can also be supplied for towing or for static use, with a maximum weight of about 780 lb. It is therefore light enough to be transported manually, if necessary.

With an a.c. unit there is no magnetic arc blow, and the arc is elastic and the striking qualities are unaffected by current setting. Fine switching ensures that there is no loss of current control.

20–250 amp a.c. welding plant developed by Welding Industries Ltd.



The unit has a welding range of 20–250 amp, enabling operation with electrodes from 16 SWG to 4 SWG. The speed of 3,000 r.p.m. gives a frequency of 450 cycles, this being particularly suitable for vertical and overhead welding.

A power take-off enables a grinding disc to be operated through a flexible shaft. It is claimed that this form of power supply reduces the cost of the equipment to about two-thirds that of a normal d.c. plant.

New wire spraying equipment

F. W. Berk & Co. Ltd. (Schori Division) have introduced their latest wire spraying equipment, the Model 58 Wire Gun, which is simple to operate and has a high performance. The gun is light and evenly balanced, having the

minimum number of moving parts, and it incorporates as a remote unit a powerful air motor with infinitely variable speed control so that there is no weight to the operator.

The Schori Model 58 Wire Gun will spray all metals produced in wire form and is suitable for all building-up work, anti-corrosion coatings and other metal spraying applications.

STUB ENDS

▶Over the next five years, Steel, Peech and Tozer Ltd. of Rotherham are to replace all their existing open-hearth steel melting furnaces by six electric arc furnaces, each of 110 tons capacity. This will become the largest electric steel-making plant in the world, with an annual capacity of 1,350,000 ingot tons.

▶The Pergamon Institute, together with part of the Pergamon Press, has moved to new headquarters at Headington Hill Hall, Oxford.

▶Construction has started on new premises near Hammersmith for the British Oxygen Co. Ltd.

▶Mr. R. H. Boughton, Sales Manager of the Welding Equipment Division of the English Electric Co. Ltd., Liverpool, lectured to the South African Institution of Welding on 16th September 1959, on the subject of "*The latest practice in a.c. arc and rectifier welding equipment*".

▶Sir Owen Wansbrough-Jones, K.B.E., C.B., has been appointed a director of The British Oxygen Company Limited.

▶Inspection Equipment Ltd. are developing a cassette which can be used for the radiography of hot welds.

▶Revised filler-metal comparison charts have been issued by the American Welding Society in booklet form. Brand names of 78 companies are listed and classified according to fifteen AWS–ASTM specifications.

▶A new quarterly export journal *EMI Electronics Post* has commenced publication with an Autumn 1959 issue. Copies can be obtained from E.M.I. Electronics Ltd., Hayes, Middlesex.

▶Lloyd's Register of Shipping have re-established an exclusive office at Plymouth (Pearl Assurance House, Royal Parade, Plymouth, Devon. Tel.: Plymouth 6700). It will be operated by Mr. R. E. Pritchard, a senior ship and engineering surveyor.

▶Members of the Welding and Fabricated Structures Panel of the Gas Turbine Collaboration Committee recently visited the Bilston works and laboratories of Quasi-Arc Limited. They were given demonstrations of manual, semi-automatic and fully automatic welding processes, and took particular interest in the applications of the latest "thin-wire" welding techniques.

▶Rhoden Partners Limited, Design and Development Engineers in mechanical and production engineering, have recently removed from 29 Park Crescent, London W.1, to larger premises at 19 Fitzroy Square, London W.1 (Tel.: Euston 9696).

▶An agreement, taking effect immediately, has been concluded between Kelvin & Hughes Ltd. and the Curtiss–Wright Corp. of America for the exclusive right to manufacture and sell the non-destructive testing equipment of the other partner. This includes instruments for the inspection and measurement of materials, and equipment employing ultrasonic techniques in all industrial fields.

▶The contract for the inspection of welds in part of the 43 mile long integration gas main of the East Midlands Gas Board has been placed with Metal and Pipeline Endurance Limited. It covers the inspection of 8 in. and 12 in. dia. steel pipe in the Lincolnshire portion of the gas distribution scheme.

Changes of Address

As from 1st January, High Duty Alloys Ltd. have closed their Hillington, Glasgow, office and a new one has been opened at De Quincey House, 48 West Regent Street, Glasgow, C.3 (Tel.: Douglas 1500).

The offices of the Executive Editor of the *British Welding Journal* (Mr. C. Rowland Harman, Editech) are now at 77a Chertsey Road, Woking, Surrey. (Tel.: Woking 5838)

DIARY

1st Mar.—Eastern Counties—"*Welding of pressure vessels and nuclear power plant*" by B. K. Barber (Eastern Electricity Board, Ipswich)
South Western—"*Welding and the draughtsman*" by H. B. Merriman (Bristol Aircraft Co. Ltd., Filton, 7.15 p.m.)

2nd Mar.—Manchester—"*Welding in the structural industry*" by A. V. Hooker (College of Technology, 7.15 p.m.)

3rd Mar.—Third Annual Lecture by W. G. John (54 Princes Gate, 6.30 p.m.)
N.E. Tyneside—"*Welding from the standpoint of the rules of the American Bureau of Shipping*" by R. T. Young (Mining Institute, Neville Hall, Newcastle, 7.0 p.m.)

7th–11th Mar.—School of Welding Technology—Course D.11/1 "*Inert-gas arc welding*"

7th Mar.—West Wales — "*Inert-gas metal-arc welding of aluminium*" by A. R. Woodward and E. M. Wilson (Swansea Technical College, 7.0 p.m.)

8th Mar.—East Wales — "*Inert-gas metal-arc welding of aluminium*" by A. R. Woodward and E. M. Wilson (Llandaff Technical College, Cardiff, 7.0 p.m.)
Liverpool—"*Uses and welding of Hastelloy*" by R. Owen (College of Technology, 7.30 p.m.)
East of Scotland—"*Preheating and stress relieving power sources*" by R. McGlashan (Lyceum Gallery, Atholl Crescent, Edinburgh, 7.30 p.m.) (joint with Edinburgh Electrical Society)

9th Mar.—South London—"*The future of gas shielded welding*" by A. A. Smith (54 Princes Gate, 7.30 p.m.)
Preston—Films and discussion evening (Harris Technical College, 7.15 p.m.)

11th Mar.—East Midlands—"*Effect of welding and stress relief on parent plate material*" by J. E. Roberts (Lincoln Technical College, 7.15 p.m.)

14th Mar.—Sheffield—"*Design, fabrication and assembly of walking dragline excavators*" by C. McL. Cameron (Grand Hotel, 7.15 p.m.)

16th Mar.—Leeds—Visit to Works of Joseph Rhodes (Wakefield)
North Eastern (Tees-side)—"*Selection and use of arc-welding processes*" by E. Flintham (Cleveland Scientific and Technical Institution, Corporation Road, Middlesbrough, 7.30 p.m.)
North London—"*Welding of low-alloy steels*" by J. F. Wilkinson (54 Princes Gate, 7.30 p.m.)
West of Scotland—"*Welding and the draughtsman*" by H. B. Merriman (Institute of Engineers and Shipbuilders, Glasgow C.2, 7.0 p.m.)

17th Mar.—Southern Counties—"*Modern chain making*" by H. O. Smerd (Technical College, St. Mary's, Southampton)

18th Mar.—Birmingham—"*High production with automatic welding*" by J. A. Lucey (Grand Hotel, 7.30 p.m.)

21st–25th Mar.—School of Welding Technology—Course D.6/2 "Welded design and construction in corrosion- and heat-resisting materials"

23rd Mar.—South London (Medway Section)—"*Developments in automatic welding with particular reference to the electro-slag process*" by A. M. Horsfield (Sun Hotel, Chatham, 7.30 p.m.)

25th Mar.—Wolverhampton—Paper on nuclear engineering by S. H. Griffiths (Wulfrunians Club, 7.30 p.m.)

27th Mar.—West of Scotland—Visit to Works of British Hydrocarbon Chemicals Ltd., Grangemouth

29th Mar.–1st Apr.—B.W.R.A. Symposium on Fatigue of Welded Structures (Engineering Dept., Cambridge University)

29th Mar.—South Western—"*Material and fabrication problems in reactor technology*" by A. Prince (Radiant House, Bristol, 7.15 p.m.)

30th Mar.—Birmingham—Visit to Corby Works of Stewarts and Lloyds Ltd.
East of Scotland—"*Welding and the draughtsman*" by H. B. Merriman (North British Station Hotel, Edinburgh, 7.30 p.m.)

5th April—East Midlands—Annual General Meeting and films (Victoria Station Hotel, Nottingham, 7.15 p.m.)
North London (Slough Section)—"*Shop inspection of welds*" (Community Centre, Slough, 7.30 p.m.)

6th April—South London (Medway Section)—Annual General Meeting (Sun Hotel, Chatham, 7.30 p.m.)
Manchester—Annual General Meeting and film show (College of Technology, 7.15 p.m.)

7th April—North Eastern (Tyneside)—"*Ship steel*" by T. W. Bushell (Mining Institute, Neville Hall, Newcastle, 7.0 p.m.)

8th April—Liverpool—16th Annual Dinner to entertain the lecturers (St. George Restaurant)
Birmingham—Annual General Meeting and technical films (Grand Hotel, 7.30 p.m.)

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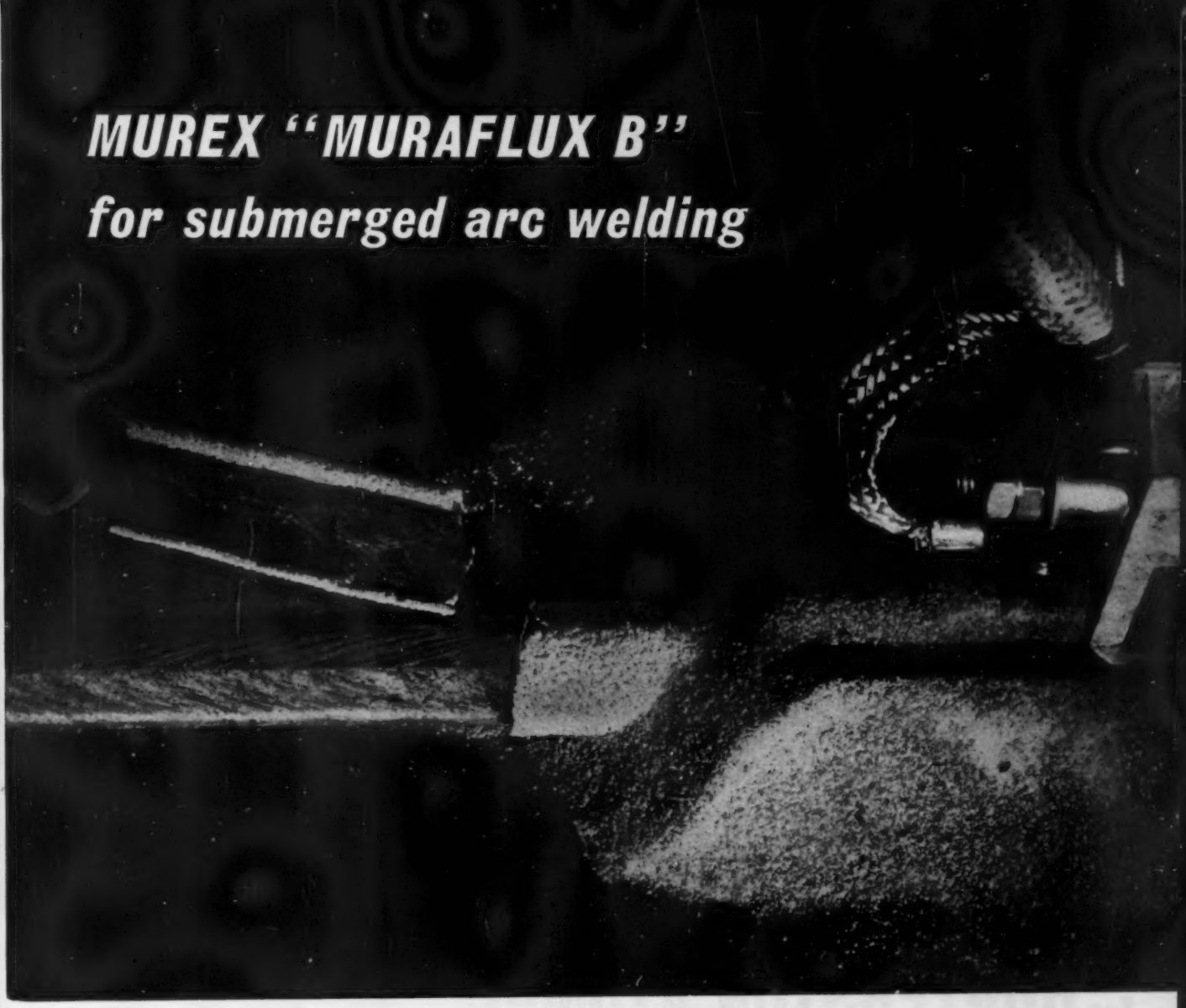

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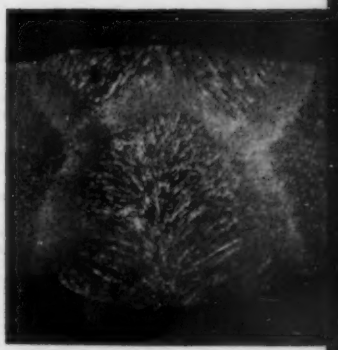

(Right) Photomacrograph of 1¼ inch thick steel plate butt welded in two passes with "Muraflux B" and ⅛ inch "Murawire W1", at 1,450 amp.

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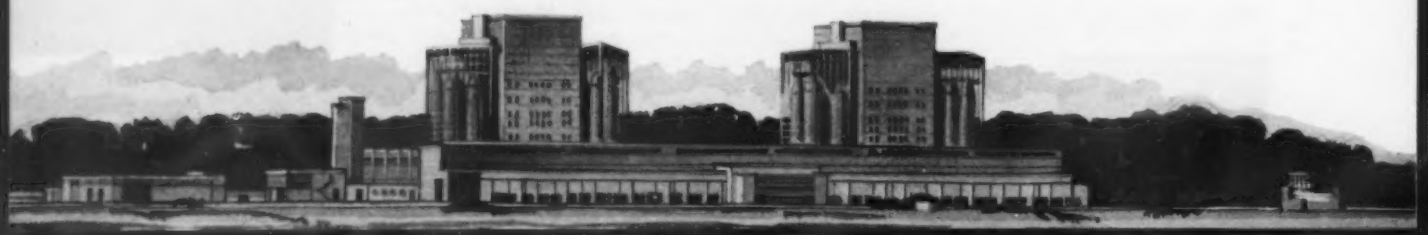

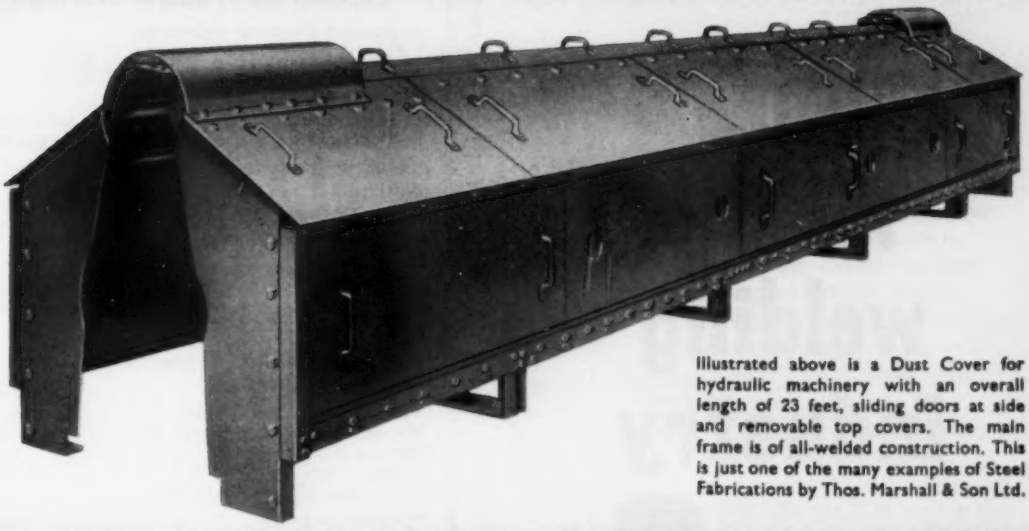

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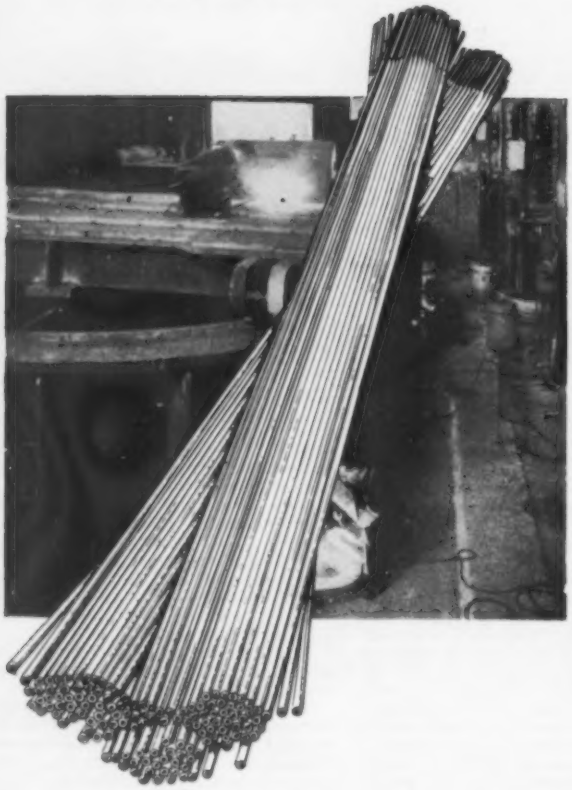

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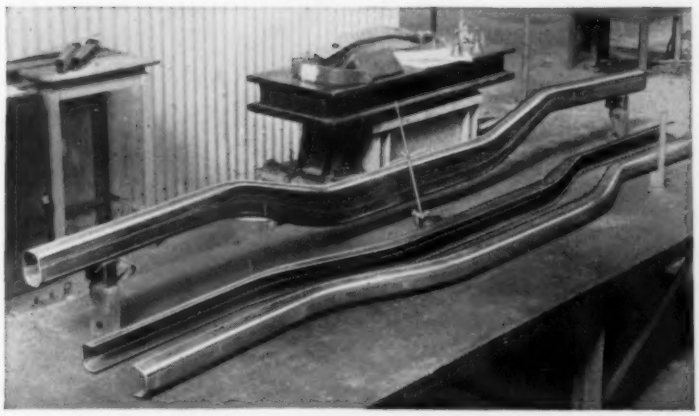
PHILIPS

bare wire CO_2 welding machine

Philips AUTOMATIC CO_2 *bare wire* welding process is now available for immediate delivery in Britain. With this machine you can speed up mass production of welded parts, for example main chassis members, bottled gas cylinders, car wheels. And you can get welds protected from nitrogen and hydrogen by an atmosphere of CO_2 — which costs only a fraction of the price of argon. The absence of coating reduces the absorption of hydrogen, too.

- Welds very wide range of steel grades.
- Needs no stocks of expensive powders.
- Cuts labour costs by welding faster, requiring little attention in repeat operations.
- Welds are homogeneous with deep and even penetration.
- Arc is self-regulating . . .
- Nitrogen and hydrogen content low — no nitrogen porosity.



Component parts and finished vehicle chassis side member welded by Philips CO_2 process. (*Photo by courtesy of John Thompson Motor Pressings Ltd.*)

Philips Automatic CO_2 welding machine

provides automatic wire and CO_2 feeds Travelling carriage moves along rails at speed set by operator. Water-cooled nozzle independent of main water supplies. Single control lever engages wire feed, switches on carriage motor and arc.

For further information about Philips CO_2 welding, and about Philips Automatic welding machine (a product of N.V. Philips, Eindhoven) write to:

Sole Distributors in the U.K.:

RESEARCH & CONTROL INSTRUMENTS LTD.

207 King's Cross Road, London. W.C.1. *Telephone : TERminus 2877.*

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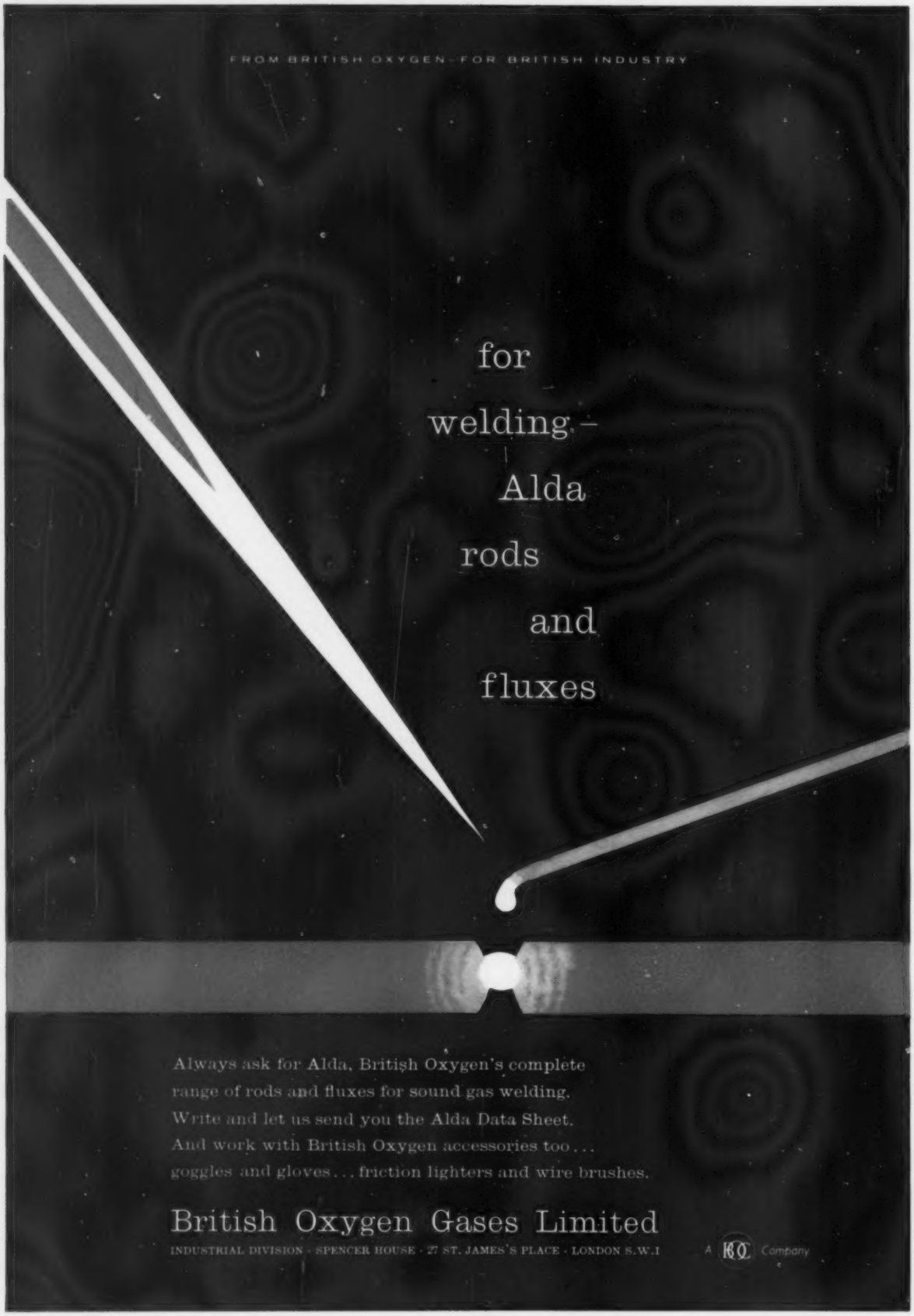
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